The WELDING JOURNAL

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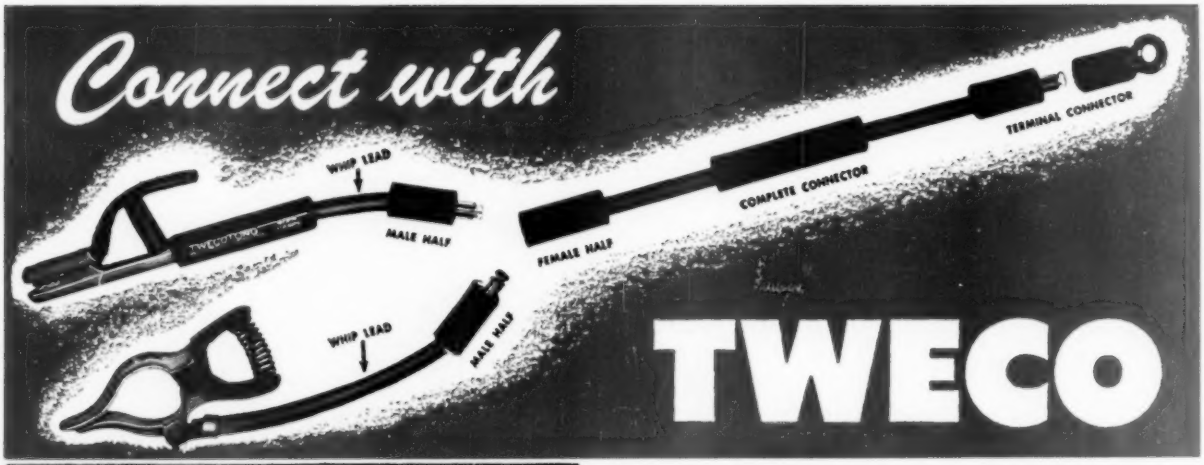

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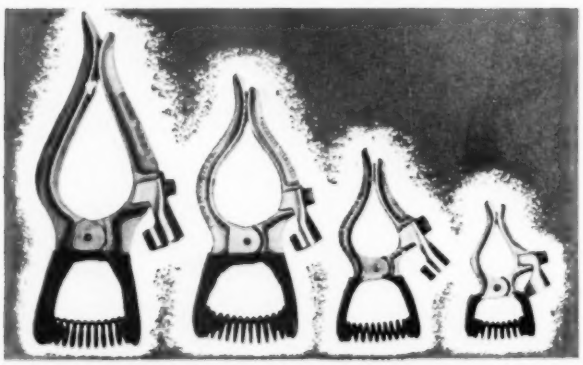

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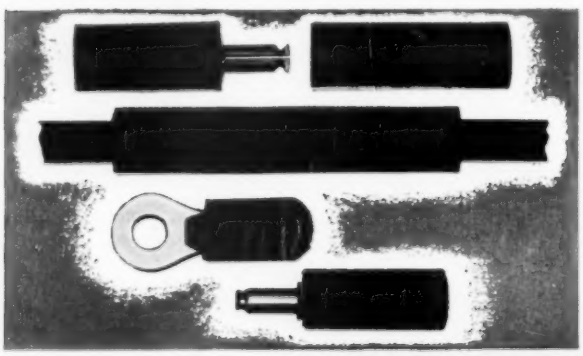

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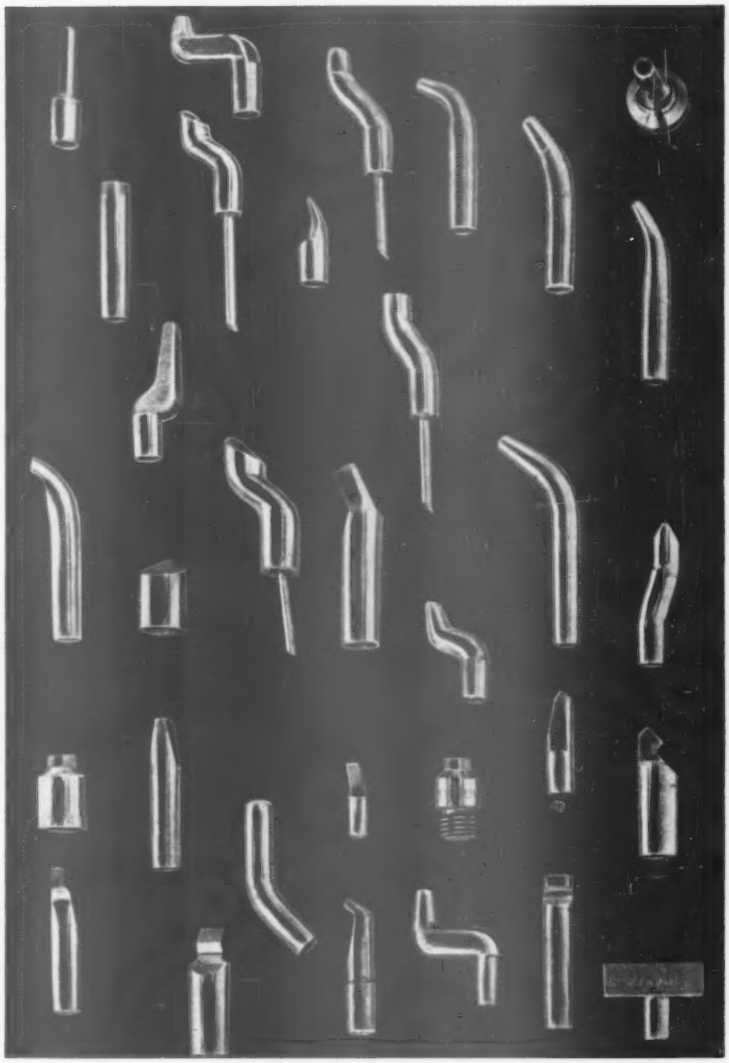

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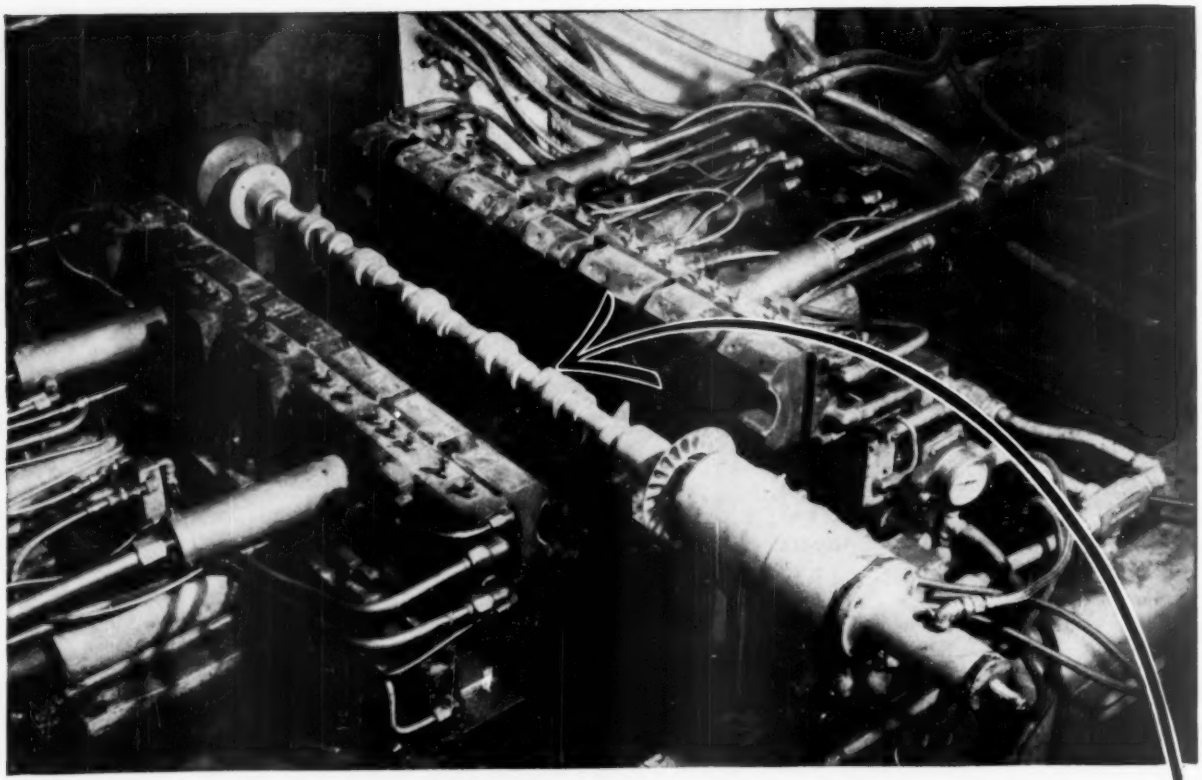

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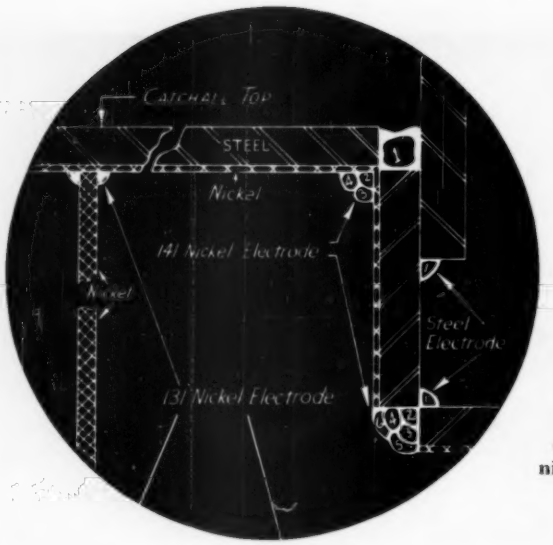

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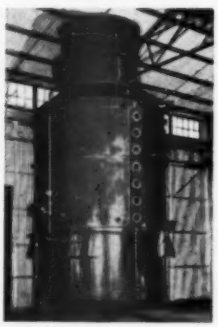

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volume 33 November 1954 number 11

Metallurgical Principles of Metal Bonding

Factors that are fundamental to making bonds between metallic pieces are discussed, and their application to various soldering, brazing and welding processes considered. The basic requirement is shown to be the bringing together of chemically clean surfaces and the establishment of intimate contact between them

by M. S. Burton

ALTHOUGH the various welding processes have been frequently described and suitable techniques discussed in great detail, the fundamental mechanisms by which metals are bonded have been generally overlooked. Often a joint is described as a "braze-type bond" or a "fusion bond," apparently with the conception that such a statement adequately defines the joining mechanism, while it, actually, only prescribes the technique of joining.

A more complete description of the principles of bonding is particularly desirable at this time, when several metals are newly joining the ranks of engineering materials. These metals introduce many new joining problems which can be better resolved when the nature of bonding is more completely understood. Successful joining of similar or dissimilar metals by any of the usual welding techniques, as well as the development of further welding methods, welding materials, and appropriate welding fluxes depends to a considerable degree upon fundamental studies of bonding mechanisms.

FUNDAMENTAL MECHANISM OF BONDING

In simplest terms, the only requirement for bonding is that atoms of the pieces to be joined be brought close enough together to allow their characteristic interatomic attraction forces to become effective. A bond between two particles utilizes the same forces that produce coherence between individual atoms within any solid metallic body. When two atoms are widely separated, negligible attraction force exists between them, but as they are brought closer together, attraction increases and becomes a maximum when the distance between the atoms is approximately twice the normal interatomic spacing, Fig. 1. Still closer spacing increases the repulsion between atoms, as between like poles of magnets, so that the net attraction force is reduced. When the attraction and repulsion tendencies are equal the net force between atoms is zero and neighboring atoms are separated by their normal interatomic spacing. If the spacing should become further reduced they tend to be mutually repulsed and forced back to their equilibrium spacing.

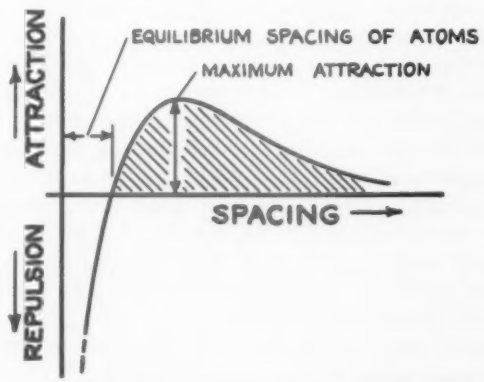

Fig. 1 Schematic representation of the forces between two atoms separated by various distances

When atoms of pieces to be joined are brought close enough together to allow the attraction force to become effective, they become drawn together until they reach the equilibrium spacing, at which time they are bonded together by the same atomic forces that hold any neighboring atoms in their proper positions. Bonding is then assured.

M. S. Burton is Associate Professor of Metallurgical Engineering, Cornell University, Ithaca, N. Y.

All of the soldering and welding techniques depend upon this type of bonding action. At the joint interface, the bond created during soft soldering is similar to the bond attained by fusion welding, and the fundamental nature of the bond between similar metals is the same as between dissimilar metals.

Proper evaluation of bonding mechanisms requires that forces necessary for bonding be considered independently of the technique utilized to increase the effectiveness of the bonding forces. However, the ease with which the prerequisites are realized with one technique, and the difficulty of attaining them with another, often determines, in a practical sense, whether a suitable bond results from a particular joining technique.

REQUIREMENTS FOR BONDING

Proper bonding action can be attained only when the joining metals are brought into intimate contact and are free from contamination at the instant of joining. Any intervening oxides, dirt, grease or adsorbed gas prevents contact between metal atoms and reduces the bonding forces. Joining may then be impossible.

The importance of cleanliness in the joint can be shown easily with simple experiments involving either metals or nonmetals. When two chemically clean glass fibers are lightly drawn over each other, small welds are repeatedly made and broken, leaving the surfaces pitted by small fractures. Although the welded areas are too small to be of practical value, this experiment does illustrate that bonding occurs when true contact is made between clean surfaces. Slight contamination of the fibers by surface films of moisture, grease, etc., completely prevents welding.

Small welds can be made in pure lead by merely touching together freshly shaved surfaces. Such welds are of minute area, but larger welds can be made by pressing the surfaces together sufficiently to produce larger areas of true contact. The metals adhere where contact is made between the two surfaces, and atomic adhesion forces are established, Fig. 2.

PRACTICAL LIMITATIONS OF THE BONDING ACTION

Serious limitations inhibit bonding in many instances. In the examples previously cited, special techniques are required to attain chemically clean surfaces. In addition, the bonding action is confined to relatively small areas, producing small joints that are unable to support large loads. To achieve practical results, the cleaning action must be simple, and joining must take place over larger areas.

Complete cleanliness of metallic surfaces can be attained only with difficulty. Thin layers of oxide or adsorbed gas almost invariably coat apparently clean surfaces, precluding bonding or greatly reducing the strength of a bond. Surface contaminants must be removed or disturbed sufficiently to present clean metal

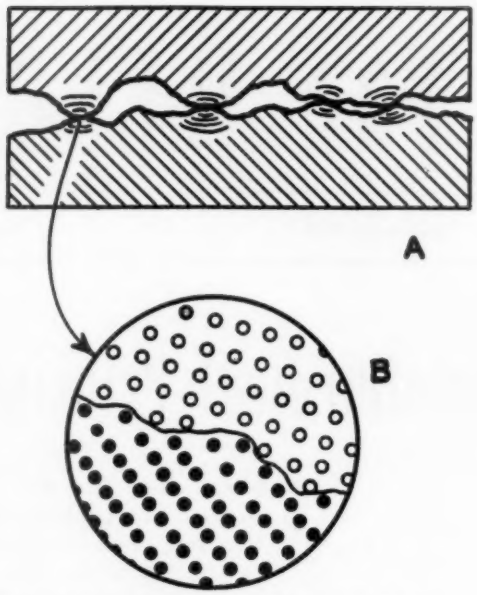

Fig. 2 Microscopical (A) and atomistic (B) representation of contact regions between two solids

(A) Surfaces of metals are somewhat rough and contact only at asperities. Bonding is limited to actual contact points rather than apparent contact areas.
(B) When true contact is realized between metals, atoms of adjoining metals become separated by essentially the equilibrium spacing, and a bond results.

for bonding at the moment the joint is being made. Various methods are used to secure clean surfaces. Heating to eliminate adsorbed gas, fluxing with gaseous or liquid fluxes, applying pressure to break a refractory oxide or to squeeze a liquid or plastic oxide from the joint, are all common steps in practical bonding techniques.

If at least one of the metals remains solid, joining must be considered in terms of the surface of the solid metal. It has been previously indicated that solids cannot be made perfectly plane and smooth, and it is obviously impossible to obtain good contact over any appreciable area by merely placing two solids together. If two ductile metals are forced together, however, the contacting asperities will be plastically deformed, and the contact area increased. Also, if one metal is liquid, it may flow over the surface of the solid and make contact over the entire surface area.

Flowing a liquid metal on a solid metallic surface will, however, not necessarily produce adequate contact between the solid and the liquid. If the liquid metal does not alloy with or wet the solid metal, only small areas of true contact can be obtained and adhesion is relatively weak. Differential contraction of the added metal and the parent metal during cooling may then completely rupture the bond.

THE NATURE OF THE BOND INTERFACE

Feasibility of any joining technique, and the mechanical properties of the joint, depend upon the nature of the material at the bond, and the type and properties

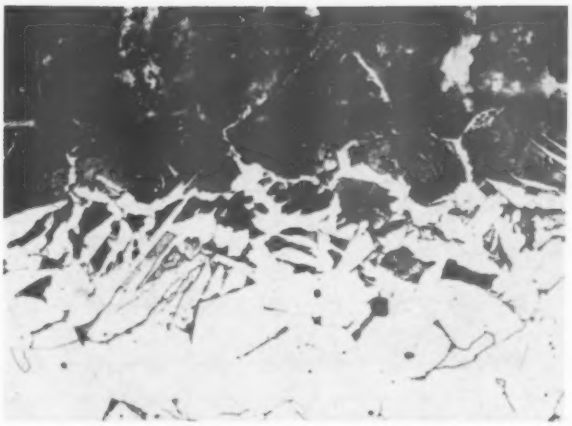

Fig. 3 Forge welded joint between AISI 1010 and AISI 1045 steel

Note the zone of carbon diffusion at the bond region, and the indications of recrystallization and coalescence at the joint interface. Nital etch, magnification × 50.

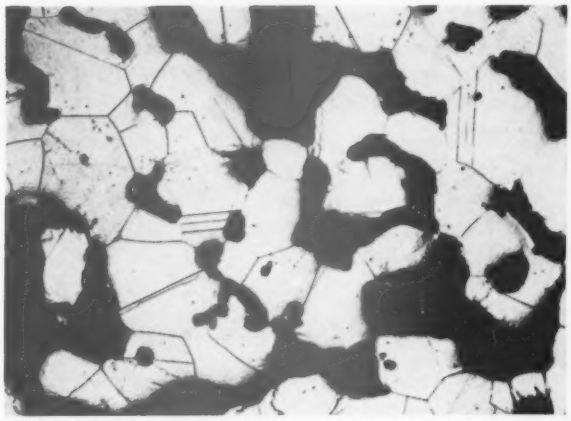

Fig. 5 Partially bonded copper particles

Pile of loose copper powder sintered in dry hydrogen at 1500° F for 1 hr, showing increase in bond areas and density due to increase of true contact area. $NH_4OH + H_2O_2$ etch. Magnification × 50.

of any alloy formed at the joint. Bonds that are perfectly sound metallurgically and chemically may be unsuitable if they are mechanically weak or excessively brittle. The nature of the bond interface is of primary importance in any evaluation of bonding.

Because metals to be bonded are generally heated during the joining operation, phase diagrams are very useful as indicators of the composition of the phases in any joint. The presence of a solid solution alloy of the metals being joined is generally indicative of a ductile bond and a metallurgically sound weld. Intermetallic compounds are almost invariably quite brittle.

The use of a phase diagram for predicting the composition of phases that are present in a joint is not always simple, and interpretation of the diagram becomes more difficult when the metals being joined are alloys rather than pure metals. In such cases the bond is usually made by one component of the joining alloy rather than by the entire alloy. One component may have greater affinity for the parent metal than another, and therefore bond more readily with the parent, forming a surface film to which the balance of the alloy easily attaches itself to complete the weld.

An example of this phenomenon is the joining of iron with lead-tin solder. Tin is necessary to produce a bond. A thin tin-iron bond forms at the interface, to which the remaining solder is able to bond. A strong bond can be made on iron with pure tin solder, but not with pure lead.

Soft solders rich in zinc have been used for many years to join aluminum. An aluminum-zinc alloy forms at the surface of the parent metal, to which ordinary lead-tin solder can be easily bonded. Although the aluminum-zinc alloy is brittle, a thin film of it in the joint has little detrimental effect on the mechanical properties of the joint.

Tungsten can be joined to iron with nickel. Nickel alloys with the tungsten forming a surface layer to which the iron can be easily bonded.

From these examples, it is evident that the previously described criterion for bonding, the close contact between atoms of the metals being joined, is greatly facilitated if one metal is in the liquid state, and if the liquid or one of its components alloys readily with the solid parent metal. The extent of alloy formation at the joint is not critical, and in many cases precautions are taken to limit alloy formation, especially if the alloy is brittle.

Bonding in the presence of a liquid phase requires that the liquid metal wet the solid metal, and when two liquid phases are present the two phases must wet each other. Complete wetting of solid metal by liquid metal entails some mutual solubility. Molten lead does not wet iron, while molten tin does. Lead-tin alloys wet iron readily. It is even possible to facilitate wetting and bonding by the deposition of metal ions from a welding flux to the parent metal.

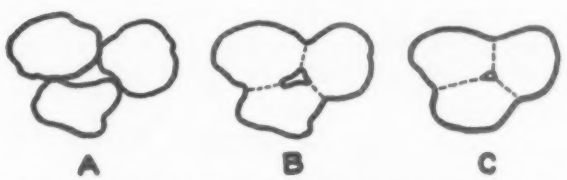

Fig. 4 Schematic representation of progress of solid-to-solid bonding between metal particles

(*A*) Particles make apparent contact in local areas, but true contact is made only at asperities in regions of apparent contact. Bonding begins at true contact areas.
(*B*) During sintering, contact areas increase and particles become rounded. Interstices become rounded, and particles fit closer together.
(*C*) Bonding between particles is nearly complete. Apparent density approaches theoretical density.

BONDING TECHNIQUES

To achieve practical welds, the foregoing theory must be implemented by particular techniques that are adapted to the conditions under which joining must be done. Similar or dissimilar metals may be bonded, sometimes while both are liquid, sometimes while one is liquid and one solid, and often while both are solid. For some welding techniques pressure is applied to the

joint, while others require no pressure. To facilitate further discussion of the nature of metallurgical bonding, the various joining techniques are considered in the following classifications:

(*a*) Cold pressure welding.
(*b*) Hot pressure welding.
(*c*) Solid-to-solid sintering.
(*d*) Liquid-to-solid bonding.
(*e*) Fusion welding.

COLD PRESSURE WELDING

Two solid metals can be bonded without the presence of an intervening liquid phase and without heating. If the surfaces of the two metals are brought into contact sufficiently well, and if no intervening film of oxide or other contamination exists, a joint is made.

It is obviously impossible to machine surfaces that are sufficiently flat and smooth to allow bonding by merely placing the surfaces in contact. The pieces must be pressed together with enough force to plastically deform the high points of the surfaces and to insure large areas of contact. Also, chemically clean surfaces become contaminated by oxide layers which form on clean surfaces and by adsorbed atmospheric gases. Such contamination may preclude welding. However, in some cases, surface contamination can be broken up by pressure so that bonding is possible at room temperature. This is particularly true of the softer metals which can be readily deformed.

At present, relatively large deformation is necessary to make cold pressure welds, a situation which limits the industrial application of this process.

HOT PRESSURE WELDING

It is not always possible to provide the proper pressure, relative motion, or plastic deformation to insure adequate bonding at room temperature, but when the temperature is raised, the technique becomes comparatively simple, as in gas pressure welding and forge welding. Yield strength of metal is reduced with rising temperature, making it possible for the butting surfaces of a joint to be easily deformed into proper contact.

For gas pressure welding, the pieces to be joined are forced together, while the region of contact is heated by gas flames. As the temperature is increased, the metal at the joint is plastically deformed, flowing together into good contact. Recrystallization of the metal usually occurs, and new crystals are formed across the joint interface. By virtue of the relatively large deformation, any contaminants at the surface are disrupted, exposing clean metal for bonding. Heating promotes bonding by facilitating plastic flow, and the high temperature allows subsequent recrystallization and coalescence at the joint.

Forge welding requires that the parts to be welded be heated and then forged together by pressure or hammer blows. This procedure is very similar to gas pressure welding, and produces the same type of joint. The parts to be joined are heated in a forge fire or furnace, to a temperature at which they are plastic but still in their solid condition, after which the surfaces to be joined are scraped clean, placed together and forged into intimate contact. No melting of either parent metal is necessary. Since the forge welding temperature is normally above the recrystallization temperature of the materials being joined, recrystallization occurs, and many of the newly recrystallized grains are born at the joint and coalesce across the joint interface, Fig. 3.

In some instances parts to be gas pressure welded may be heated until a thin film of liquid metal forms at the welding surface. However, the welding pressure squeezes the liquid from the joint, producing solid-phase bonding as described above.

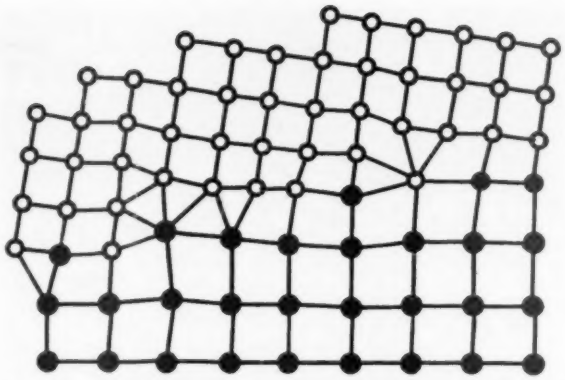

Fig. 6 Two-dimensional schematic representation of coherence and bonding between adjacent dissimilar phases. Some atomic dislocations must occur at the bond to allow different lattices or differently oriented lattices to fit together

SOLID-TO-SOLID SINTERING

Metals to be joined in the solid state need not be forced together by external pressure. They may be joined by prolonged heating below their melting temperature. Although this technique is of particular importance in powder metallurgy and is not generally considered to be a welding method, the bond is the same as in any of the usual welding methods.

Bonding takes place only when there is metal to metal contact, and since no external pressure is applied, contact can only be increased by diffusion and surface tension. During heating, the areas in contact become larger as surface tension acts to round the sharp corners of regions not in intimate contact. Internal holes become smaller and more rounded, and in the ex-

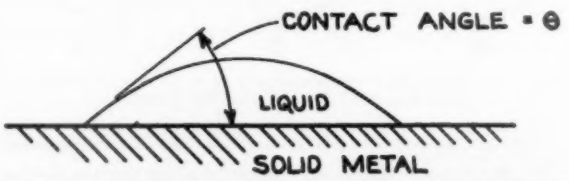

Fig. 7 Variations of contact angle with tendency for liquid metal to wet and spread on the surface of the solid

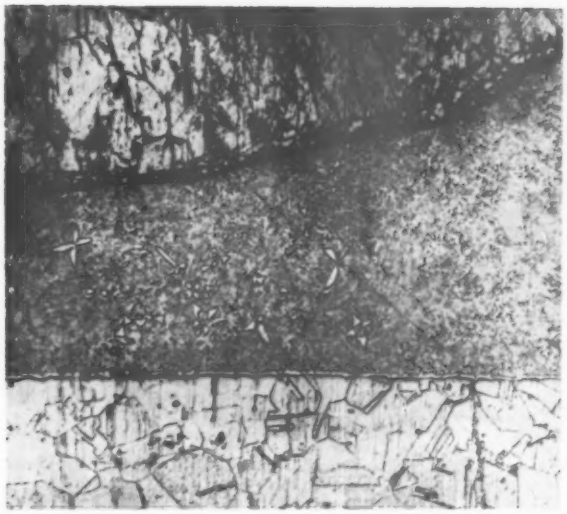

Fig. 8 Silver brazed joint between steel (top) and alpha brass (70% Cu, 30% Zn, bottom) showing alloy formation at interfaces. $NH_4OH + H_2O_2$ etch, followed by 2% Nital. Magnification × 100

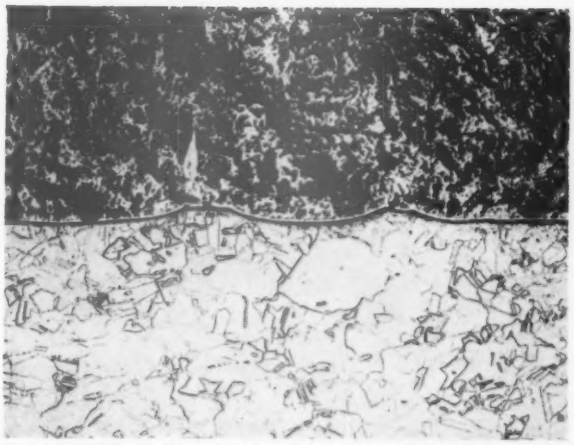

Fig. 9 Soft soldered joint in brass. Palmerton etch. The 50 % lead–50 % tin solder is bonded to the 80 % Cu–30 % Zn brass by a tin-rich alloy at the interface. Magnification × 200

treme case, internal holes are healed so that essentially complete densification is effected. This action is schematically shown in Fig. 4, and the metallographic appearance of a bond between solid copper particles after prolonged heating without external pressure is shown in Fig. 5.

Bonding of solid to solid is common in metals. Any two phase alloy has large areas of such a bond where the two phases join each other. Why do two such phases hold together? The basic criterion is that a coherent structure be developed at the interface, Fig. 6. Coherence implies that atoms of the two phases come together in exactly the manner that has been previously discussed for the ordinary welding method. In many two-phase alloys, one of the phases is precipitated within the matrix of the other phase. Any precipitation is via a diffusion mechanism, atoms diffusing through the solid metal to coalesce as a precipitate at a particular region. The second phase is nucleated and grows within the matrix of the first phase, and coherence exists at all times during the nucleation and growth process.

LIQUID TO SOLID BONDING

When the bonding temperature is higher than the melting temperature of one of the metals being joined, or higher than the melting temperature of an alloy of these metals, a liquid phase is present during bonding, and must be considered. This is the situation when soldering or brazing.

It has been stated by other writers that a braze bond depends upon wetting of the solid metal by the braze metal. When the conditions for bonding liquid to solid are realized, wetting automatically occurs, yet wetting is not the sole requirement for bonding. Wetting of solid metal by a liquid requires an interfacial tension such that the angle of contact between the solid and liquid is small, Fig. 7, and for complete wetting of the solid this angle must be zero. In practical cases wetting of a solid by a liquid metal is only realized when there is some solubility of the solid in the liquid. When the solubility is negligibly small, wetting does not occur to an extent sufficient for bonding.

Wetting connotes spreading, but it is not necessary to have complete spreading. Liquid copper is able to spread on clean iron and the contact angle of liquid copper against the iron is zero. Tin is able to spread to a lesser degree on iron because of its greater contact angle. However, both copper and tin can be adequately bonded to iron.

At the interface between the liquid and the solid a minor amount of alloying occurs so that the atoms at the joint interface are properly bonded to both the liquid and the solid. As the liquid begins to freeze, the first solid forms at the joint interface and grows toward the hotter liquid metal, exactly the same as a casting in a mold cavity. A cross section through a silver brazed joint which illustrates this phenomena is shown in Fig. 8 and a similar section of a soft soldered joint is shown in Fig. 9. In both of these photomicrographs it may be seen that the bonding action takes place between the solid metal and the first crystals to solidify from the liquid metal, which are not of the same composition as the nominal analysis of the braze metal or solder. Thus, it is important to consider the composition of the first metal to solidify, since that is the material which must normally bond with the parent metal.

When the solid metal has appreciable solubility in the liquid joining metal, and limited solubility in the same metal after its solidification, the mechanics of bonding are somewhat altered. During bonding some of the solid is dissolved in the liquid, and during subsequent cooling, when its solubility is decreased, it precipitates from the liquid at the solid-liquid interface. The precipitate bonds itself to the solid metal and is automatically coherent with the original solid metal. This situation is identical with precipitation of a solid phase within the matrix of a liquid phase during the

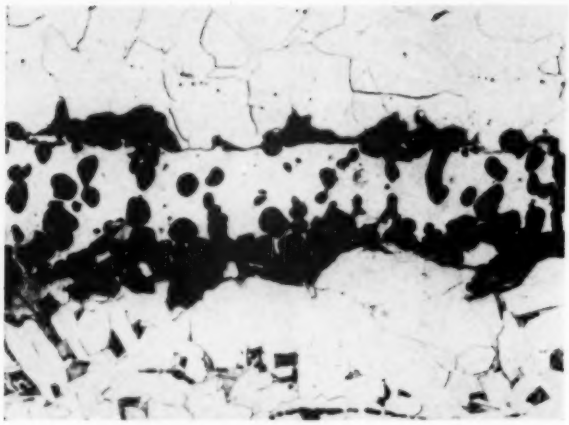

Fig. 10 Copper brazed joint in steel

Dark rounded particles in braze metal are iron-copper precipitate. Diffusion of copper into parent metal is shown by dark bands at the bond. The rounded appearance of the steel surface indicates partial solution of parent metal in the copper braze metal. Nital etch, 2%. Magnification × 200.

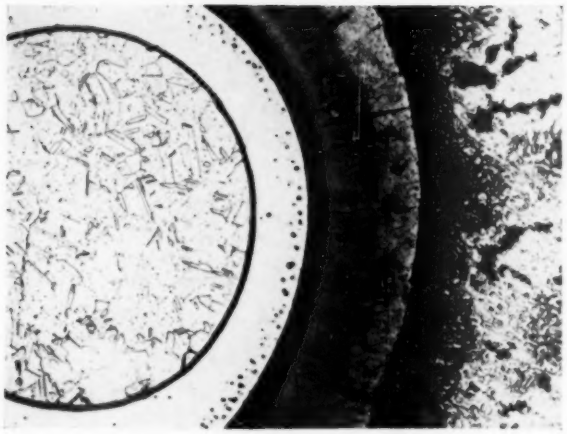

Fig. 11 Copper-zinc bond formed below melting temperature of zinc

Note that a great amount of interdiffusion between copper and zinc has taken place and that several rather thick alloy layers have been formed. Copper wire at left. Pure zinc at right, copper-zinc alloys in between. $NH_4OH + H_2O_2$ etch. Magnification × 75.

cooling of a two-phase alloy. The undissolved solid tends to seed the precipitation of the supersaturated material from the liquid, and the precipitate first forms at the interface between the liquid and the solid, Fig. 10.

Two metals may be bonded below the melting point of either but above the melting temperature of an alloy of the two. When salt is thrown on ice during the wintertime, no liquid exists until the ice and the salt contact each other forming a water-salt "alloy" which has a lower freezing temperature than pure water. An analogous situation exists at the interface of a joint heated below the melting temperature of either of the parent metals, but above the melting temperature of an alloy of the two. An intermediate liquid phase is formed. Interdiffusion at the joint creates a composition that becomes liquid during the bonding procedure. Since the liquid is an alloy of the two joining metals it will bond readily to each. A joint of this type is illustrated in Fig. 11. Copper and zinc have been joined at temperatures considerably below the melting temperature of either. At the interface a complete series of copper-zinc alloys exists.

FUSION BONDING

Practically all metals and alloys can be joined to similar metals by fusion welding. The metals are melted, flowed together, and solidified, creating a weld region of cast metal securely attached to the parent metal. Cooling of the liquid metal is principally by heat flow to the solid parent metal and hence the solidification of the liquid normally starts at the region where the fused metal and the parent metal join. Atoms of the fused metal attach themselves to the already existing crystal structure of the parent metal, producing columnar dendrites that grow toward the center of the joint until all the liquid metal is frozen and the weld is complete. The melted metal begins to solidify at the surface of the solid parent metal and must be coherent with that surface. In most cases close examination of photomicrographs of a fusion welded joint discloses evidence that coherence is readily attained. In Fig. 12, it may be noted that the grains of the parent metal at the fusion line control the crystallographic orientation of the freezing dendrites. At the line of fusion the dendrites often have the same oriented luster as the contiguous parent grains and appear to be merely continuations of the parent grains. These two features are definite indications that the dendrites are coherent with the parent metal.

At the instant when bonding first occurs, the melting metal begins to solidify by attaching itself to the cooler parent metal already at hand, and a moment later other atoms solidify from the liquid by attaching themselves to the growing dendrites. Since the strength of the bond between adjacent atoms is never questioned, why should the bond between the solid parent metal and the first atom layers of the metal solidifying against it be regarded with suspicion? The bond is strong and con-

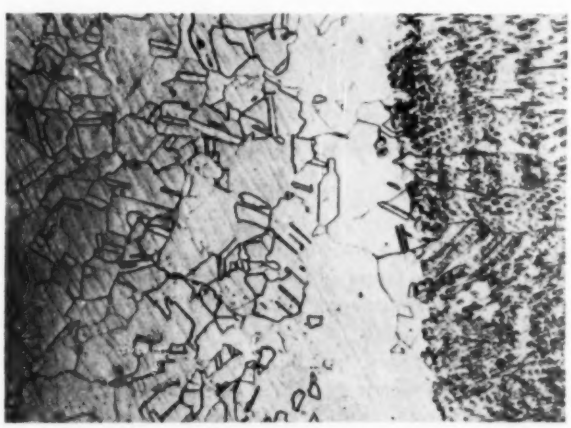

Fig. 12 Coherence at bond in stainless steel metallic arc weld

Orientation of parent metal at joint interface is continued into the weld metal, as indicated by continuation of grain boundaries through the interface and the absence of any boundaries that would tend to follow the interface. Electrolytic H_2CrO_4 etch. Magnification × 100.

tinuous and is identical with the bond between any adjacent atoms.

In the multipass weld shown in Fig. 13 the columnar dendrites of the first weld pass continue the crystal orientation of the parent metal with which they were bonded, and subsequent weld passes continue the orientation of the original dendrites through several weld beads without change. The absence of sharp demarkations between individual passes is indicative of the accuracy of atomic orientation at each bond between the various passes of the completed weld. Also, the influence of solid metal upon subsequently deposited liquid is very evident. Bonds between individual weld passes of a multipass weld are never questioned, yet they are identical with the bond between the first pass of a fusion weld and the parent metal.

As has been previously explained, the surfaces of the liquid metal and the solid metal must be perfectly clean. During heating, most metals form surface oxides with oxygen from the atmosphere and the oxides generally have a higher melting temperature than the parent metal, inhibiting bonding of the liquid metal to the parent metal. For such metals a flux is generally necessary to remove surface oxides and to protect the cleaned metals from further oxidation during heating. However, when the metallic oxide has a lower melting temperature than the parent metal, as in the case of carbon steel, the oxide is easily displaced by the added metal and does not interfere with the bonding action, and flux is unnecessary. As is well known, a flux is not needed for fusion welding the low-carbon and low-alloy steels.

Although fusion welding of a metal with a liquid metal of similar composition appears quite simple, the mechanism by which a parent metal is fusion bonded with a filler metal of different composition may not be completely obvious. How can a fusion bond be formed between carbon steel and stainless steel, materials having different melting temperatures and considerably different composition? At the interface between two joining metals, liquid melted from the parent stock mixes with the weld metal to provide a gradient composition. The composition gradually changes from the analysis of the parent metal to that of the weld metal, so that the bond region has a gradient composition. The solidifying metal gradually alters its composition from that of one parent metal to the nominal analysis of the welded metal. At no place is there an exceedingly sharp gradient of composition when considered on an atomic scale. In most cases high magnification examination with an optical microscope reveals the gradient composition at the joint interface. In other cases dilution of the weld metal by the parent, and the diffusion of the two metals into each other, is not as great and therefore the diffusion region is narrow. However, gradient compositions can be revealed by suitable techniques.

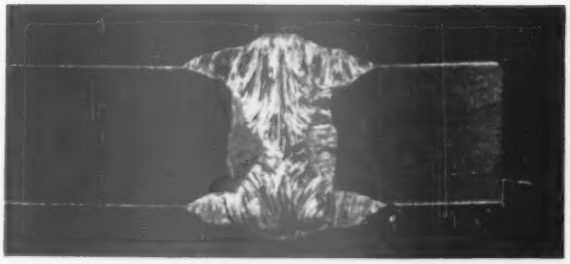

Fig. 13 Multipass metallic arc weld in stainless steel showing coherence between passes

The crystalline orientation of each weld pass is determined by the continuation of etching luster through several weld passes. Etch $FeCl + HCl + 0.5\% HNO_3$. Magnification × 1½.

In this discussion it has been assumed that the materials are of relatively similar composition and that no metallic compounds are formed by the intermixing of the weld metal and the parent metal. If one or more compounds of the parent metal and the added metal are present the joint may be brittle, and it may be impossible to make useful fusion bonded joints. Other welding techniques can be then more advantageously employed.

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(*Continued on page 1079*)

Spot Welding Aluminum Alloys with Single-Phase Equipment

The data on spot welding of aluminum alloys to MIL-W-6860 specification using single-phase a-c spot-welding equipment

by J. F. Harris

Synopsis

This paper presents the basic weld schedule data of single-phase a-c spot welding of aluminum alloys using a shaped wave form of Up-Slope and Down-Slope currents along with forging pressure.

Five gages of 24S-T3 Alclad, 0.016 through 0.091; four gages of 61S-T4, 0.020 through 0.102; and three gages of 52S-H34 are covered in the paper.

The prime purpose is to list the weld force, weld current and time of each part of the sequence that will produce single-phase aluminum spot welds that unquestionably qualify to MIL-W-6860 specifications. Supporting data such as photomacrographs, test results, etc. are presented, together with a set of recommended weld schedules. Direct inking type oscillograms indicating the magnitude and timing of each function are included so that better translation of the data can be made to other types of equipment.

INTRODUCTION

THE spot welding of aluminum alloys to MIL-W-6860 specification using three-phase equipment of either the low-frequency converter type or the dry disk rectifier type is an established production practice. Tentative schedules have already been issued by the AMERICAN WELDING SOCIETY in its bulletin *Recommended Practices for Spot Welding Aluminum and Aluminum Alloys.*

The purpose of this paper is to indicate that the spot welding of aluminum alloys in conformance with MIL-W-6860 specification with all welds free of cracks and defects and with an average shear value 15% above specification is entirely feasible when using single-phase a-c equipment provided the recently developed tools such as Up-Slope and Down-Slope for shaping the current wave and forging pressure are used.

Three different aluminum alloys were selected for this program. These alloys were 24S-T3 Alclad in 5 gage thicknesses of 0.016–0.016 in., 0.020–0.020 in., 0.040–0.040, 0.064–0.064 and 0.091–0.091; alloy 61S-T4 in 4 gage thicknesses of 0.020–0.020, 0.040–0.040, 0.064–0.064 and 0.102–0.102 in. and alloy 52H-34 in 3 gage thicknesses of 0.040–0.040, 0.064–0.064 and 0.102–0.102 in.

These alloys are widely used and require different welding techniques. Alloy 24S-T3 Alclad represents a heat-treatable alloy in the hard condition; alloy 61S-T4 represents a heat-treatable alloy in the soft condition; alloy 52S-H34 represents an alloy which is considered nonheat treatable in the sense of determining its physical properties.

In this program, it was not intended to undertake an exhaustive research program in which each welding variable was investigated independently of the other variables. A different approach was used based on past experience of welding aluminum on both dry disk rectifier d-c equipment and low-frequency converter equipment.

From this past work, the values of electrode forces, current timing and current magnitudes could be approximately established as well as the application of forging pressure with respect to the current wave. With this knowledge, a preliminary schedule would be tried and, based on its results, the appropriate variables would then be changed to result in an insensitive schedule.

After having established a satisfactory schedule, each variable would be varied from the schedule value with the other variables held constant as noted in the schedule. The degree of variation and the results from the variation are noted after the weld schedules. This procedure is similar to that outlined in MIL-W-6860 specification in which it specifies that when using a schedule which has produced qualified spot welds in the past it is permissible to vary *two* welding variables 5% or *one* variable 10% without the necessity for requalification.

FINAL DATA

Satisfactory weld schedules for the three alloys in the various thicknesses are given in tabular form in Tables 2, 3 and 4. The sensitivity of these schedules when one variable is changed is also noted after each set of schedules. Recordings of each current pattern used are included to avoid misunderstanding of the terms. Photomacrographs are included to show weld structure and to assist in measuring the weld dimensions. The average shear strength, standard deviations factor and

Table 1—Degreasers and Deoxidizers Used in Surface Preparation of 24S-T3 Alclad for Spot Welding

0.016	Cowles AM	160° F	10	Weldal	190° F	5	3–10
0.020	Oakite 61	175° F	10	Diversey 514	70° F	10	10–27
0.040	Oakite 61	186° F	10	Diversey 514	72° F	10	20–30
0.064	Oakite 61	176° F	10	Diversey 5A	70° F	10	8–35
0.091	Oakite 61	170° F	10	Diversey 514	68° F	15	20–35

Degreasing followed by hot water rinse (130–140° F) for 2 min. Deoxidizing followed by cold water rinse 60–65° F for 2 min followed by drying with an air blast.
Cowles AM, Cowles Chemical Co., Cleveland, Ohio.
Diversey 514, The Diversey Corp., Chicago, Ill.
Weldal, Thone, Inc., New Haven, Conn.

J. F. Harris is Welding Engineer, Taylor-Winfield Corp., Warren, Ohio.

Presented at the AWS National Spring Meeting held in Buffalo, N. Y., May 4–7, 1954.

Table 2

WELDING SCHEDULES 24ST-3 ALCLAD ALUMINUM					
MATERIAL THICKNESS INCHES	.016 - .016	.020 - .020	.040 - .040	.064 - .064	.091 - .091
CHEMICAL DEOXIDIZER	WELDAL	DIVERSEY 514	DIVERSEY 514	DIVERSEY 514	DIVERSEY 514
CONTACT RESISTANCE MICROHMS	3 - 30	10 - 27	20 - 30	8 - 35	20 - 35
WELDER TAP SWITCH SETTINGS	S - 6	S - 7	P - 4	P - 5	P - 8
WELD HEAT DIAL SETTING	92	92	92	87	90
UP-SLOPE HEAT DIAL SETTING	20	40	40	40	40
DOWN-SLOPE HEAT DIAL SETTING	NOT USED	30	35	50	70
WELD TIME-CYCLES 60 CPS	1	2	5	11	17
UP-SLOPE TIME-CYCLES 60 CPS	1	1	1	3	4
DOWN-SLOPE DELAY TIME-CYCLES 60 CPS	NOT USED	1	2	5	8
FORGE DELAY TIME-CYCLES 60 CPS	1	1	2	5	8
DOWN-SLOPE TIME-CYCLES 60 CPS	NOT USED	1	2	5	5
HOLD TIME-CYCLES 60 CPS	15	15	15	30	30
INITIAL ELECTRODE FORCE LBS	500	500	700	1180	1700
FINAL ELECTRODE FORCE LBS	1200	1200	1600	2750	4300
UP-SLOPE CURRENT-SECONDARY AMPS	5500	8500	10800	16850	17700
WELD CURRENT-SECONDARY AMPS	17700	19800	28300	34500	46600
DOWN-SLOPE CURRENT-SEC AMPS	NOT USED	11300	10600	18700	33200
ELECTRODE MATERIAL	CLASS 1	CLASS 1	CLASS 1	CLASS 1	CLASS 1
ELECTRODE DIAMETER IN	7/8	7/8	7/8	7/8	7/8
ELECTRODE RADIUS DOME IN	3	3	3	6	6

Table 2A

TEST RESULTS 24ST-3 ALCLAD ALUMINUM					
MATERIAL THICKNESS INCHES	.016 - .016	.020 - .020	.040 - .040	.064 - .064	.091 - .091
NUMBER OF TEST WELDS-TENSILE	100	100	100	100	100
AVERAGE TENSION SHEAR LBS	162	228	578	1126	2039
MIL-W-6860 MIN AUG SHEAR LBS	145	187	460	920	1700
% OVER REQUIRED MIN AUG SHEAR	12	22	26	22	20
MINIMUM SHEAR OF TEST LOT LBS	147	200	508	968	1680
MAXIMUM SHEAR OF TEST LOT LBS	185	254	623	1256	2260
% WITHIN ± 5% OF AUG	72	68	67	67	85
% WITHIN ± 10% OF AUG	97	95	97	92	97
% WITHIN ± 12 1/2% OF AUG	99	100	100	97	99
STANDARD DEVIATION	10	15	32	48	84
COEFFICIENT OF VARIATION	6.2	6.5	5.5	4.3	4.2
PENETRATION %, 5 SECTIONS	38-76	30-62	52-71	50-67	43-76
AUG NUGGET DIAMETER INTERFACE	.134	.143	.156	.281	.334
EXTERNAL IMPRINT DIAMETER	.156	.200	.266	.391	.464
SURFACE INDENTATION IN	.001	.001	.001-.002	.004-.005	.007-.009
SHEET SEPARATION IN	.001	—	.003	.003	.007
RADIOGRAPHIC DEFECTS	NONE	NONE	NONE	NONE	NONE
MACROSCOPIC DEFECTS	NONE	NONE	NONE	NONE	NONE
METALLOGRAPHIC SECTION NO	H-40	H-32	H-23	H-24	H-31
COUPON WIDTH AND OVERLAP IN ACCORDANCE TO MIL-W-6860 TEST COUPONS CUT FROM MULTIPLE SPOT PANELS					

coefficient of variation based on a group of 100 welds and calculated in accordance with ASTM *Manual on Presentation of Data* as well as other physical properties of the weld are presented in Tables 2A, 3A and 4A.

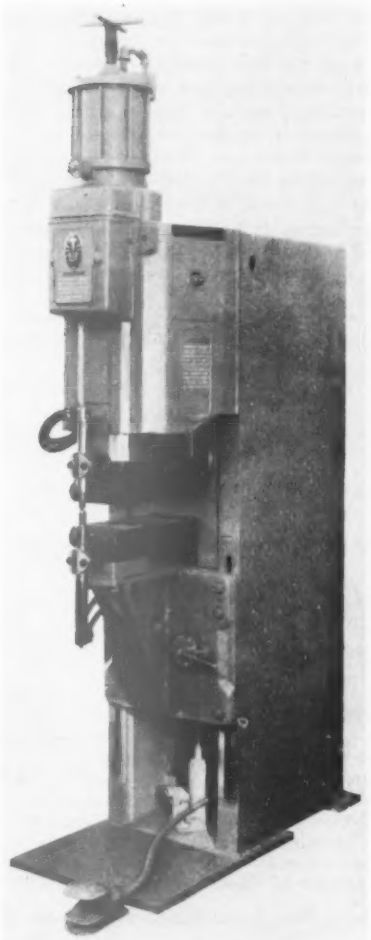


Fig. 1 Press welder used in welding program

WELDING EQUIPMENT

The welder used in this work was a Taylor-Winfield, Type ERB-12-150 kva (RWMA Size 2) direct-acting air-operated press welder. It was equipped with a roller bearing ram and a dual pressure

Table 2B—Effect of Changing the Forge-Delay Time When Welding 24S-T3 Alclad

Gage thickness, in.	*Schedule value, cycles*	*—Decreased value—* *Cycles*	*%*	*Effect of decrease*	*—Increased value—* *Cycles*	*%*	*Effect of increase*
0.016–0.016	1	0.1	90	Dud welds—shear strength decreased 60%	2	100	No apparent change in weld characteristics
0.020–0.020	1	..	..	.....	2	100	No apparent change in weld characteristics
0.040–0.040	2	1	50	Internal structure of welds satisfactory. Shear strength reduced 7%	3	50	No apparent change in weld characteristics
0.064–0.064	5	4	20	Internal structure of welds satisfactory. Shear reduced 5%	6	15	No apparent change in weld characteristics
0.091–0.091	8	6	25	Internal structure of welds satisfactory. Shear strength reduced 15%	10	20	No apparent change in weld characteristics

Considering the effect of the changes, it can be summarized that the Forge-Delay time or forge pressure may be applied at the end of Down-Slope Delay time or later (up to 20%) without apparent ill effect but, if applied before the end there will be a slight decrease in shear strength.

air lock consisting of a seal-off rolling diaphragm combination interposed between ram and operating cylinder. The arrangement is such that the welding force resulting from the smaller airlock area subtracts from the force exerted by the larger airlock area during the low, initial or welding force period and is exhausted at high speed for the application of final or forging force. The time from initiation of final force to a value of 75% of maximum of final force is 10 msec which indicates the rapid rate of response possible with this system. This is shown by oscillograms as Fig. 4.

The welder is equipped with a 150-kva single-phase, a-c transformer having a secondary voltage range from 3.23 to 9.15 v in 16 steps obtained by use of an 8-step tap switch and a series parallel tap switch. The maximum short circuit secondary current with 18-in. throat depth and 8-in. horn spacing is 59,000 amp.

A photograph of the welding apparatus is shown in Fig. 1.

The control apparatus was a standard Taylor-Winfield, Type NS-2HUNF-A-G1 synchronous a-c control. This control contained a NEMA 7B sequence of Squeeze, Hold and Off times and a synchronous-precision weld timing section plus a precision timed Up-Slope control, a precision timed Down-Slope control and a precision timed Forge-Delay control. A photograph of the control and the various adjusting dials is shown in Fig. 2.

Table 2C—Effect of Changing the Weld Current (Max) When Welding 24S-T3 Alclad*

Gage thickness, in.	*Schedule value, amp*	*—Decreased value—* *Amp*	*%*	*Effect of decrease*	*—Increased value—* *Amp*	*%*	*Effect of increase*
0.016–0.016	17,700	16,300	8	No apparent change in weld characteristics	19,500	10	No apparent change in weld characteristics, but electrode sticking was evident
0.020–0.020	19,800	18,600	6	Stick welds, shear strength decreased 40%	21,200	7	Internal structure of welds satisfactory but shear strength increased 9%
0.040–0.040	28,300	26,200	9	Stick welds, shear strength decreased 45%	29,500	4	Internal structure of welds satisfactory but shear strength increased 10%
0.064–0.064	34,500	31,500	9	Internal structure of welds satisfactory but shear strength reduced 27%	38,000	10	Internal structure of welds satisfactory but shear strength increased 18%
0.091–0.091	46,600	43,200	7	Internal structure of welds satisfactory but shear strength reduced 25%	50,000	7	First weld on panel expelled metal or "spit," shear strength on others increased 10%

* While it was the intent to change only one variable at a time, it is not possible to do this if the weld current is changed by adjusting the weld heat dial (or phase shift). The Up-Slope current and Down-Slope current change proportionally unless their dials are readjusted. In the above table, they were not readjusted so it can be assumed that the Up-Slope and Down-Slope currents change proportionally to the weld current.

Considering the above effects from weld current change, the shear strength is closely related to the weld current value. For proper operation, the current should be at the schedule value or slightly higher. Even then a 5% increase in weld current can be expected to result in a 10% increase in shear strength.

Table 2D—Effect of Changing the Up-Slope Time When Welding 24S-T3 Alclad

Gage thickness, in.	*Schedule value, cycles*	*—Decreased value—* *Cycles*	*%*	*Effect of decrease*	*—Increased value—* *Cycles*	*%*	*Effect of increase*
0.016–0.016	1	*	..	*	*	..	*
0.020–0.020							
0.040–0.040							
0.064–0.064	3	2	33	No apparent change in weld characteristics	4	25	No apparent change in weld characteristics
0.091–0.091	4	2	50	Weld would expel metal when welding or "spit"	..	..	.....

* The Up-Slope time could only be varied in increments of one cycle which would have resulted in a 100% variation in these gages. Being this variation also affects the flow of maximum weld current, this large variation would obviously produce pronounced changes in weld characteristics. It is not to be inferred that smaller changes such as those resulting from changing the wave shape would have the same effect.

Table 2E—Effect of Changing the Down-Slope Delay Time (or Weld Heat Time) When Welding 24S-T3 Alclad

Gage thickness, in.	*Schedule value, cycles*	*—Decreased Value—* *Cycles*	*%*	*Effect of decrease*	*—Increased value—* *Cycles*	*%*	*Effect of increase*
0.020–0.020	1	*	..	*	*	..	*
0.040–0.040	2	*	..	*	*	..	*
0.064–0.064	5	..	..	....	6	20	The penetration increased about 15% and the shear strength approximately 13%
0.091–0.091	8	..	..	.....	9	13	The penetration increased and the welds would expel metal or spit while welding

* This time interval includes the Up-Slope time and the time when the maximum welding current is flowing. In these gages, when the time is so short and the possible incremental adjustment is 1 cycle, the large percent variation would obviously cause a pronounced change in weld quality.

This time interval is the most critical of all time intervals used in the welding schedules and therefore should be precisely timed in a welding control.

Table 2F—Effect of Changing the Down-Slope Current When Welding 24S-T3 Alcald

Gage thickness, in.	*Schedule value, amp*	*—Decreased value—* *Amp*	*%*	*Effect of decrease*	*—Increased value—* *Amp*	*%*	*Effect of increase*
0.020–0.020	11,300	...	..	.....	13,000	15	No internal defects in weld. Shear strength increased 10%
0.040–0.040	10,600	9,100	14	No apparent effect on weld quality or shear strength	16,000	50	No internal defects in weld. Shear strength increased 5%
0.064–0.064	18,700	7,500	40	Welds are cracked. Shear strength unchanged	23,500	26	No apparent effect on weld quality or shear strength
0.091–0.091	33,200	28,100	15	No apparent effect on weld quality or shear strength	39,000	17	Welds expelled metal or "spit" while welding
0.091–0.091	33,200	24,500	26	Welds are cracked. Shear strength unchanged	...	..	.....

From examination of the above effects the Down-Slope current can be decreased about 15% without any apparent effect but caution should be exercised in increasing the value.

Table 3

WELDING SCHEDULES 61ST-4 ALUMINUM				
MATERIAL THICKNESS INCHES	.020-.020	.040-.040	.064-.064	.102-.102
CHEMICAL DEOXIDIZER	WELDAL	WELDAL	COWLES DO	WELDAL
CONTACT RESISTANCE MICROHMS	3-8	7-27	8-15	3-23
WELDER TAP SWITCH SETTING	P-1	P-2	P-4	P-7
WELD HEAT DIAL SETTING	96	90	92	90
UP SLOPE HEAT DIAL SETTING	20	40	40	40
DOWN SLOPE HEAT DIAL SETTING	NOT USED	35	50	60
WELD TIME CYCLES 60 CPS	1	5	11	20
UP SLOPE TIME CYCLES 60 CPS	1	2	3	4
DOWN SLOPE DELAY TIME CYCLE 60 CPS	NOT USED	3	5	11
FORGE DELAY TIME CYCLE 60 CPS	1	3	5	11
DOWN SLOPE TIME CYCLE 60 CPS	NOT USED	2	5	5
HOLD TIME CYCLES 60 CPS	15	30	50	60
INITIAL ELECTRODE FORCE LBS.	500	550	900	1430
FINAL ELECTRODE FORCE LBS.	1200	1370	2150	3500
UP SLOPE CURRENT SEC. AMPS.	6,300	7,200	10,100	14,700
WELD CURRENT SEC. AMPS.	19,000	23,200	30,100	46,000
DOWN CURRENT SEC. AMPS.	NOT USED	9,350	16,200	27,000
ELECTRODE MATERIAL	CLASS I	CLASS I	CLASS I	CLASS I
ELECTRODE DIAMETER IN.	7/8	7/8	7/8	7/8
ELECTRODE RADIUS DOME IN.	3	6	6	6

Table 3a

TEST RESULTS 61ST-4 ALUMINUM				
MATERIAL THICKNESS IN.	.020-.020	.040-.040	.064-064	.102-.102
NUMBER OF TEST WELDS TENSILE	100	100	93	100
AVERAGE TENSION SHEAR LBS.	217	505	875 *	1472 *
MIL-W-6860 MIN. AVG. SHEAR LBS.	133	400	754	1267
% OVER MIN. AVG. SHEAR	63	26	16	16
MINIMUM SHEAR OF TEST LOT	198	455	770	1330
MAXIMUM SHEAR OF TEST LOT	246	560	1012	1614
% WITHIN ± 5% OF AVG.	81	68	58	74
% WITHIN ±10% OF AVG.	97	96	93	100
% WITHIN ±12-1/2% OF AVG.	99	100	99	100
STANDARD DEVIATION	16.9	23	57	45
COEFFICIENT OF VARIATION	7.7	4.6	6.5	3.1
PENETRATION %, 5 SECTIONS	36-60	32-65	48-61	53-73
AVG. NUGGET DIA. INTERFACE	.159	.200	.252	.328
EXTERNAL IMPRINT DIA.	.172	.278	.391	.500
SURFACE INDENTATION IN.	.002-.003	.0017-.0034	.004-.005	.006-.007
SHEET SEPARATION IN.	.001	.001	- - -	.006
RADIOGRAPHIC DEFECTS	NONE	NONE	NONE	NONE
MACROSCOPIC DEFECTS	NONE	NONE	NONE	NONE
METALLOGRAPHIC SECTION NO.	H-33	H-34	H-35	H-36

* FIRST WELDS ON PANELS HIGHER THAN 25% OVER AVERAGE, ALL FIRST WELDS DISCARDED BEFORE ABOVE AVERAGE WAS TAKEN

COUPON WIDTH AND OVERLAP IN ACCORDANCE TO MIL-W-6860. TEST COUPONS CUT FROM MULTIPLE SPOT PANELS.

The ranges and adjustments of the various control functions are as follows:

Squeeze time, 3–120 cycles
Hold time, 3–120 cycles
Off time, 3–60 cycles
Weld time, 1–60 cycles
Forge-Delay, 1–72 cycles
Up-Slope time, 1–15 cycles
Down-Slope delay time, 1–60 cycles
Down-Slope time, 1–15 cycles

Independent heat dials for the current adjustment of the initial current, weld current and final current magnitudes are provided.

EXPLANATION OF TERMS IN THE WELDING SCHEDULE

Various names have been assigned to the functions referred to as Up-Slope and Down-Slope in this paper. While these terms have now been standardized by a joint RWMA-NEMA Committee on Definitions, the terms are not yet in common usage nor are the adjustments on the welding controls now being manufactured so identified.

The terms as used in this paper are shown in Fig. 3. Changes to conform to the new standard have not been made. The deviations are as follows:

1. Down-Slope delay changed to weld-heat time
2. The difference of the weld time and weld-heat time is identified as postheat time.

MEASUREMENT OF VARIABLES IN THE WELDING SCHEDULES AND TEST EQUIPMENT

A U-type electrode force gage made by the General Electric Co. was used periodically to measure the static electrode force.

The dynamic electrode force was measured with a Brush strain analyzer, Model BL310 and a direct inking Brush oscillograph, Model BL202. Examples of such measurements are shown in Fig. 4. The single-phase welding current caused pronounced pickup or interference with the strain gage measurement so that it was

Table 3B—Effect of Changing the Forge-Delay Time When Welding 61S-T4

Gage thickness, in.	Schedule value, cycles	—Decreased value— Cycles	%	Effect of decrease	—Increased value— Cycles	%	Effect of increase
0.020–0.020	1	..	..	.....	2	100	No apparent change in weld characteristics
0.040–0.040	3	2	33	No apparent change in weld characteristics	..	..	....
0.064–0.064	5	4	25	Internal structure of welds satisfactory. Shear strength reduced 9%	4	25	Internal structure of welds satisfactory. Shear strength increased 4%

Considering the effect of the changes, it can be summarized that the Forge-Delay time or forge pressure should be applied at the end of Down-Slope Delay time or later (up to 20%) without apparently changing the weld characteristics appreciably. However, if the forge pressure is applied before the end of Down-Slope delay, there may be a decrease in shear strength.

Forge pressure, considering weld quality only, may not be necessary on the 0.020–0.020 in. thickness. Sample checks at a constant electrode force of 500 lb (single pressure) equivalent to the initial force on double pressure schedule, as noted in Table 3, did not produce any obvious changes in weld characteristics.

Table 3C—Effect of Changing the Weld Current* (Max) When Welding 61S-T4

Gage thickness, in.	Schedule value	—Decreased value— Amp	%	Effect of decrease	—Increased value— Amp	%	Effect of increase
0.040–0.040	23,200	21,500 22,200	7 4	Internal structure of welds satisfactory. Shear strength decreased 35% at 21,500 amp and 5% at 22,200	24,000 24,500	4 6	Internal structure satisfactory at 24,000 amp. Shear strength increases 19%. At 24,500 amp spit welds with expelled metal resulted. Shear increased 19%
0.064–0.064	30,100	29,400	3	Internal structure satisfactory. Shear strength reduced 23%	30,800	3	Internal structure satisfactory. Shear increased 6%
0.102–0.102	27,000	23,000	15	Internal cracks. No change in shear	33,500	24	Internal structure satisfactory. Shear increased 14%

* For 0.040–0.040 and 0.064–0.064 in. material.

The above results indicate that the reduction of welding current by 3 to 7% below schedule value may reduce shear value by up to 35%. Increase of current by 6% results in spit welds.

The schedule used for the 0.102–0.102 material is apparently less sensitive although 15% reduction in current results in cracked welds, current may be raised 24% with only 14% increase in shear.

Table 3D—Effect of Changing the Up-Slope Current When Welding 61S-T4

Gage thickness, in.	Schedule value	—Decreased value— Amp	%	Effect of decrease	—Increased value— Amp	%	Effect of increase
0.020–0.020	6,300	..	..	.....	9,000	43	Internal structure satisfactory. Shear increases 18%
0.040–0.040	7,200	..	..	.....	10,200	14	Internal structure satisfactory. Shear increases 34%
0.064–0.064	10,100	7200	29	Internal structure satisfactory. No change in shear	..	..	.....

To summarize the data above, it appears that fairly large changes may be made in Up-Slope current without seriously affecting weld quality. Increases above the schedule value result in increased shear and since the heating effect of Up-Slope current is small compared to the total weld, the changes in shear are small.

necessary to make force measurements without current flow when a more accurate picture of dynamic force was required.

Up-Slope, Down-Slope and Weld Currents

In all cases, the secondary current was calculated from the primary current by multiplying the primary current by the turns ratio of the welding transformer for the particular tap setting being used.

The primary current was measured on a Brush oscillograph, Model BL202 using a Brush a-c amplifier, Model BL905, and a suitable current transformer and shunting resistor. The primary current resulting in the weld current was further checked by making a measurement with a pointer stop ammeter such as a Weston type A3 Model 633. In these measurements, neither Up-Slope nor Down-Slope currents were used. This cross checking was performed as welding current in general, was a portion of a phase-shifted wave and it is known that the Brush instruments do not accuractely record phase-shifted waves.

The calculation of the initial value of the Up-Slope current, referred to as Up-Slope current in the body of this paper, and the final value of the Down-Slope current, referred to as Down-Slope current in the remainder of this report, was performed by using the weld current as a base. If, for example, the weld current from the Brush record was indicated as 15 mm deflection and known to be 15,000 amp from the pointer stop ammeter measurement (and in general from the Brush measurements as phase back was least during this period), and the initial Up-Slope current indicated 5 mm deflection on the Brush record, the Up-Slope current was assumed to be (5/15) × 15,000 or 5000 amp. The Down-Slope current was calculated in the same manner.

This method of calculation does introduce an error as the Brush instrument does not read either a true rms or peak value over the wide range of phase shift used in this work. However, the current values were not corrected for this error; it is generally less than 10%.

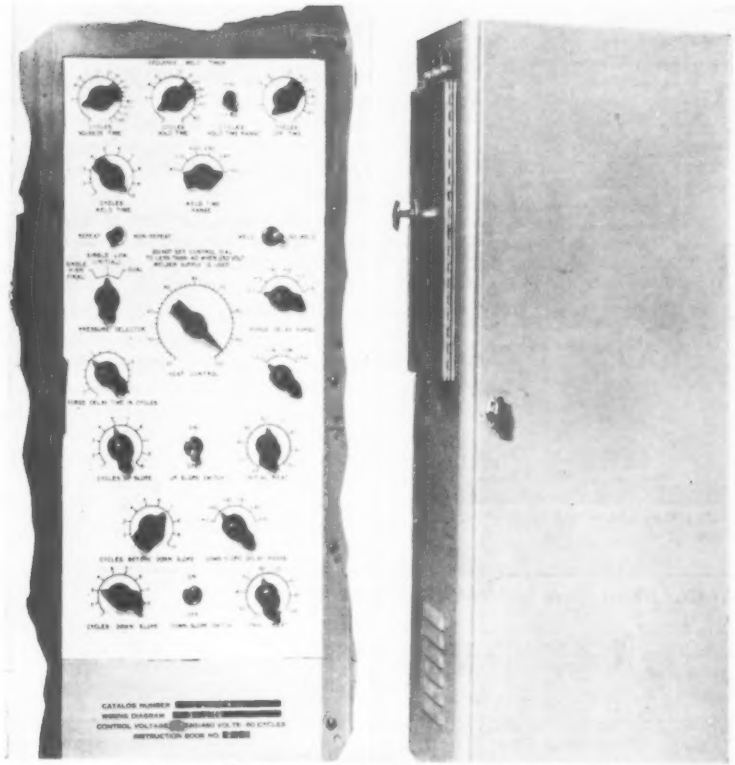


Fig. 2 Electronic control and adjustment panel

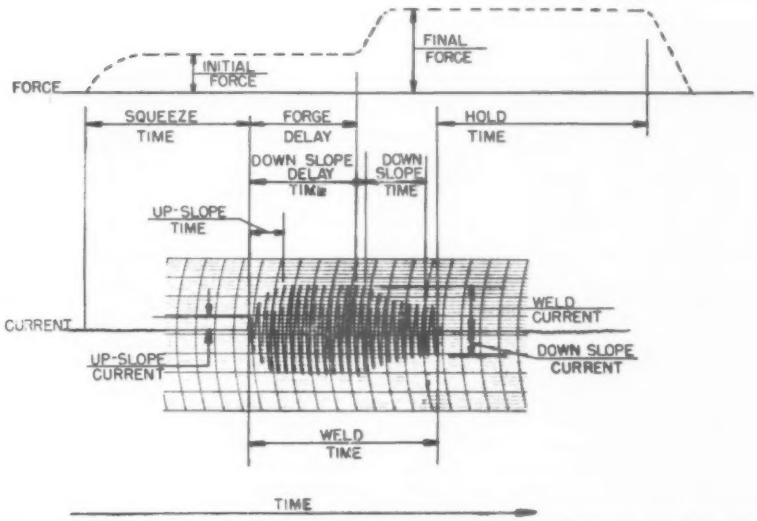


Fig. 3 Force, current and time relationship definition of terms used in this report

Up-Slope, Down-Slope and Weld Times

The time measurements were made from the same Brush oscillograms as the current. There will always be a degree of confusion on exactly what the Up-Slope and Down-Slope times are unless the initial and final heat levels are at approximately their minimum settings and the weld current heat level at its maximum setting. With this condition, the times are more clearly evident. In this report, they have been taken as the time the current reaches 90% of its final value. To avoid any confusion, Brush oscillograms for each gage thickness are shown in Figs. 5, 6 and 7.

TENSILE SHEAR TESTS

The tensile shear tests were made in a conventional manner from coupons as specified in MIL-W6860 specification on a Baldwin Southwark hydraulic testing machine with dual force ranges to 2000 and 20,000 lb max.

Contact Resistance

The contact resistance was checked in a press exerting 1000 lb force through a set of RWMA, Class 1, No. 2 Morse Taper electrodes with 3-in. radius domed faces. The electrical and measuring source was a double Kelvin bridge which supplied approximately 30 d-c amp during the measurement.

Radiographic Tests

All welds were X-rayed in a conventional manner to determine the extent of cracks, porosity or other internal defects. Photomacrographs were made for each thickness of an alloy in a conventional manner. For every schedule, at least 5 weld sections or macrosections were made to allow measurement of nuggets and penetration.

Results

The reporting of results will be divided into three sections, each one covering a specific alloy.

SPOT WELDING OF 24ST3 ALCLAD

SURFACE PREPARATION

Various types of degreasers and deoxidizers were used on this alloy all of which gave satisfactory surface preparation for spot welding.

The basic procedure was to degrease, rinse in hot water of 130–140° F temperature for 2 min, deoxidize in the etchant, rinse in cold water of 60–65° F for 2 min and dry by air blast.

The various degreasers and etchants used are shown in Table 1.

WELDING SCHEDULES

The satisfactory welding schedules are shown in Table 2 and the physical results of the welds are shown in Table 2A.

The internal weld structure is evident from the photomacrographs indicated in Fig. 8. The weld current patterns are shown in Fig. 5.

Electrode Cleaning

No attempt was made to determine what variables could be changed to increase the number of welds between cleaning the electrodes. This is an exhaustive subject in itself. However, at least 50 welds were made between electrode cleaning on all gages reported in Table 2. When welding 0.020 in. thickness, slight sticking to the electrodes was evident after the twenty-fifth weld. In some cases, a thin coating

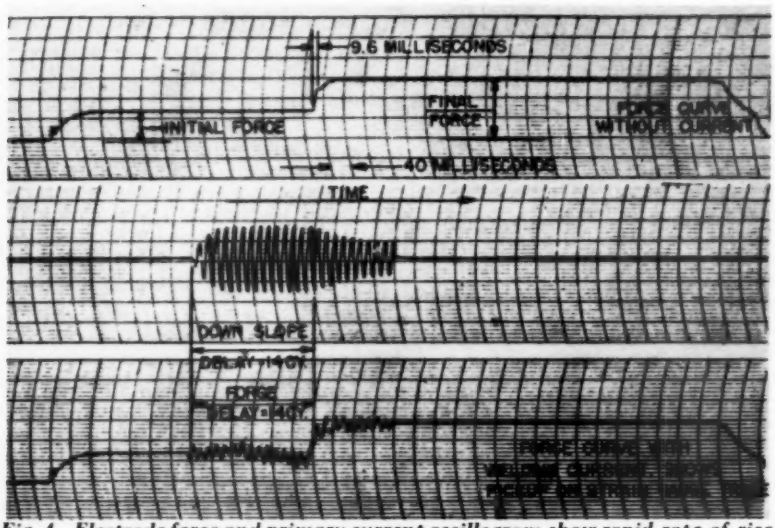


Fig. 4 Electrode force and primary current oscillogram show rapid rate of rise from initial to final force

table, is being changed. The other variables remain as indicated in Table 2.

The effect of the change is based on a sample of 3 to 10 welds for each change. A short summary or explanation follows each table indicating the general effect of the variable and any pertinent facts concerning the method of change of the variable.

The variable of Up-Slope or initial current could be increased as much as 20% in all gages without any apparent effect on weld quality. Therefore, the changing of this variable is not listed in the tables.

The variable of Weld time could be changed as much as 22% without any effect on weld quality. It should be realized that such a change only reduces the degree of postheating. The weld has already been made. The changing of this variable is not listed in the tables.

Other variables changed for specific cases were the electrode force and elec-

Table 3E—Effect of Changing Down-Slope Current When Welding 61S-T4

Gage thickness, in.	*Schedule value, amp*	*—Decreased value— Amp*	*%*	*Effect of decrease*	*—Increased value— Amp*	*%*	*Effect of increase*
0.040–0.040	9,350	...	..	.....	15,500	18.6	Internal structure satisfactory. Shear increased 8%
0.064–0.064	16,200	11,700	28	Internal structure satisfactory. Shear reduced 6%	18,400	13	Internal structure satisfactory. Shear value unchanged
0.102–0.102	27,000	23,000	20	Internal cracks. No change in shear	33,500	22	Internal structure satisfactory. Shear increased 14%

A reduction in Down-Slope current of 20% may result in defective welds, while an increase of up to 22% may be made with only 14% increase in shear and no apparent change in internal structure.

Table 3F—Degreasers and Deoxidizers Used in the Surface Preparation of 61S-T4 for Spot Welding

Thickness in.	*Degreasers type*	*Temperature ° F*	*Time min*	*Deoxidizers type*	*Temperature ° F*	*Time min*	*Contact resistance microhms*
0.020 0.020	Ethone NE	165	10	Weldal	186	10	2.6–7.8
0.040–0.040	Ethone NE	170	10	Weldal	185	6	6.8–27
0.064–0.064	Cowles AM	170	10	Cowles DO	130	6	7.5–15
0.102 0.102	Cowles AM	175	10	Weldal	185	8	2.2–23

Degreasing followed by hot water rinse (130–140° F) for 2 min. Deoxidizing followed by cold water rinse (60–65° F) for 2 min (black smut removed occasionally) followed by drying in air blast.
Cowles AM and DO, Cowles Chemical Co., Cleveland, Ohio.
Enthone NE and Weldal, Enthone, Inc., New Haven, Conn.

of aluminum on the electrodes was evident at the end of a run of 50 welds but no trace of copper could be detected on the aluminum surface when etched with Kellers' etch.

It should be noted that the weld schedule for 0.016–0.016 in. thickness indicates 1 cycle Up-Slope time and also 1 cycle Weld time. The current was thus starting at its minimum value and increasing during the complete Weld time* of one cycle. There was reason to believe (described in detail under spot welding 61S-T4) that the use of this technique did increase the number of welds possible before cleaning the electrodes. This technique was not used on 0.020–0.020 in. thickness as this data had already been compiled before the 0.016–0.016 in. thickness had been investigated. It is possible that the slight sticking occurring during the 0.020–0.020-in. thickness run could have been eliminated had this procedure been used.

* The current only reached 90% of its maximum present value possible had the Weld time beed increased one half cycle.

SENSITIVITY OF WELDING SCHEDULES WHEN SPOT WELDING 24S-T3 ALCLAD

The sensitivity of the various variables has been recorded in the form of tables for easier reading. These tables are numbered 2B, 2C, etc., indicating they all apply to the Weld schedule as shown in Table 2.

It should be recalled that only one variable, that used as the heading for the trode face radius. On 0.016–0.016 in. thickness, reduction of the electrode force from 500 to 400 lb caused more electrode sticking without any other apparent effect on weld quality. The electrode face radius dome was changed from 3 to 6 in. on the 0.064–0.064 in. thickness as the indentation and sheet separation were both excessive when using 3 in.

SPOT WELDING 61S-T4 SURFACE PREPARATION

The preparation of the surfaces of this alloy by chemical cleaning for spot welding is a difficult problem. Several deoxidizers were tried which would produce low contact resistance on some sheets of this alloy but not on all sheets. It was found neces-

sary to check each lot of materials in each gage to insure uniform surfaces for welding.

The principal difficulty seems to be the appearance of the surface before cleaning. If it is bright and smooth, the cleaning seems to be satisfactory. If it is dark and rough, the cleaning is more difficult and almost impossible in some cases.

It is our understanding that some fabricators of this alloy have adopted a practice of processing this alloy in a nitric-hydrofluoric acid bath and thereby deep etching it prior to storage in their plant. When the alloy is to be used for welding, it is cleaned with one of the standard cleaning procedures. The deep etching apparently leaves a uniform white surface which is not seriously affected during storage time.

The basic procedure used in this work was to degrease, rinse in hot water from 130 to 140° F for 2 min, deoxidize in the etchant, rinse in cold water for 2 min and dry in an air blast.

It was necessary at times to wipe off a black smut resulting from the deoxidizer during the final rinse operation.

The various degreasers and etchants used are shown in Table 3 F. It should be noted that the contact resistance is less than 30 microhms in all cases. Some preliminary spot welding was performed on 0.064-in. thick material having a fairly uniform contact resistance of 200 microhms. It was not possible to obtain satisfactory schedules as surface metal expulsion or "spitting" resulted. Four or five spot welds could be made with the electrodes tending to adhere to the sheets more each time and then expulsion would occur between the sheet and electrode.

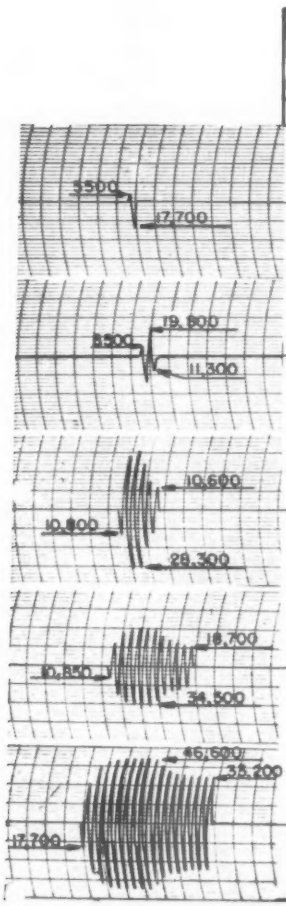
When the cleaning was not uniform, there being high and low values of contact resistance along the sheets where welds were to be made, a characteristic resulting from too short an immersion time in the deoxidizer, the resultant tensile shear values were also very erratic.

From these results, it was evident that uniform and low-contact resistance is required for successful spot welding with single-phase a-c much the same as that required by any other method.

While considerable effort wa expanded to uniformly clean the 61S-T3 material, it was not possible to obtain the desired level of uniformity.

Table 4

WELDING SCHEDULES 52S-H34 ALUMINUM			
MATERIAL THICKNESS INCHES	.040-.040	.064-.064	.102-.102
CHEMICAL DEOXIDIZERS	WELDAL	WELDAL	WELDAL
CONTACT RESISTANCE MICROHMS	6-18	4-7	4-18
WELDER TAP SWITCH SETTING	S-8	P-2	P-4
WELD HEAT DIAL SETTING	90	90	97
UP SLOPE HEAT DIAL SETTING	40	40	40
DOWN SLOPE HEAT DIAL SETTING	50	50	70
WELD TIME CYCLES 60 CPS	5	14	17
UP SLOPE TIME CYCLES 60 CPS	2	3	4
DOWN SLOPE DELAY TIME CY. 60 CPS	3	8	8
FORGE DELAY TIME CY. 60 CPS	3	8	8
DOWN SLOPE TIME CY 60 CPS	2	5	5
HOLD TIME CYCLES 60 CPS	15	30	60
INITIAL ELECTRODE FORCE LBS	700	900	1600
FINAL ELECTRODE FORCE LBS	1600	2150	3900
UP SLOPE CURRENT SEC. AMPS	6600	7840	15700
WELD CURRENT SEC. AMPS	18200	26400	33000
DOWN SLOPE CURRENT SEC. AMPS	9600	12900	27700
ELECTRODE MATERIAL	CLASS 1	CLASS 1	CLASS 1
ELECTRODE DIA. IN.	7/8	7/8	7/8
ELECTRODE RADIUS DOME IN.	6	6	6



SHEET THICK	WELDER CONTROL SETTINGS							
	HEAT DIAL			TIME				
	UP SLOPE	WELD	DOWN SLOPE	UP SLOPE	DOWN SLOPE DELAY	DOWN SLOPE	WELD
.016	20	92		1			1
.020	40	92	30	1	1	1	2
.040	40	92	35	1	2	2	5
.064	40	87	50	3	5	5	11
.091	40	90	70	4	8	5	17

Fig. 5 Oscillograms of primary current. 24S-T3 Alclad

WELDING SCHEDULE

The satisfactory welding schedules are shown in Table 3 and the physical results of these welds are shown in Table 3A.

The internal weld structure is evident from the photomacrographs indicated in Fig. 9.

The weld current patterns are shown in Fig. 6.

In order to attain qualification schedules on the 0.064–0.064 in. and 0.102–0.102 in. thicknesses, it was necessary to eliminate the first weld on a qualification sheet (not shunted) from the results.

These welds invariably were beyond the limit of the allowable 25% change from the average being of higher shear strength. Such a procedure was not necessary on other gages of this alloy.

As a practical thing, it would mean that the first weld of each sheet should be made at a reduced heat setting of weld current

Table 4a

TEST RESULTS 52S-H34 ALUMINUM			
MATERIAL THICKNESS INCHES	.040-.040	.064-.064	.102-.102
NUMBER OF TEST WELDS TENSILE	100	100	100
AVERAGE TENSION SHEAR LBS	513	913	1901
MIL-W-6860 MIN. AVG. SHEAR LBS.	415	835	1600
% OVER REQUIRED MIN. AVG. SHEAR	24	9	19
MINIMUM SHEAR OF TEST LOT LBS.	460	778	1690
MAXIMUM SHEAR OF TEST LOT LBS.	573	1062	2400
% WITHIN ± 5% OF AVG.	72	60	62
% WITHIN ±10% OF AVG.	98	83	91
% WITHIN ±12-1/2% OF AVG.	100	95	92
STANDARD DEVIATION	24	64	150
COEFFICIENT OF VARIATION	4.6	7.0	7.9
PENETRATION %, 5 SECTIONS	37-53	42-67	48-67
AVG. NUGGET DIA. INTERFACE	.141	.245	.282
EXTERNAL IMPRINT DIA.	.234	.344	.438
SURFACE INDENTATION IN.	.0010-.0015	.001-.004	.004-.005
SHEET SEPARATION IN.	.001	.001	.005
RADIOGRAPHIC DEFECTS	NONE	NONE	NONE
MACROSCOPIC DEFECTS	NONE	NONE	NONE
METALLOGRAPHIC SECTION NO.	H-39	H-38	H-37
COUPON WIDTH AND OVERLAP IN ACCORDANCE TO MIL-W-6860. TEST COUPONS CUT FROM MULTIPLE SPOT PANELS			

if it is to be used in the averaging of the samples.

Electrode Cleaning

No serious effort was made to determine what welding variables could be changed to increase the number of welds before cleaning the electrodes. As stated previously, regardless of the surface preparation of the sheets used for welding, it was felt that it could be improved but it was beyond the knowledge of the writer to improve it. This fact gave rather peculiar results as far as electrode cleaning was concerned.

At least 50 welds were made between cleanings on 0.020–0.020, 0.064–0.064 and 0.102–0.102 in. thicknesses. In some cases, the electrodes tended to stick before the 50 welds were made but no trace of copper could be detected on the aluminum surface when etched with Kellers' etch. However, it was not found possible to spot weld the 0.040–0.040 in. thickness satisfactorily unless the electrodes were cleaned after every 15 welds.

Table 4B—Effect of Changing Forge-Delay Time When Welding 52S-H34

Gage thickness, in.	*Schedule value, time*	*—Decrease value—* *Time*	*%*	*Effect of decrease*	*—Increased value—* *Time*	*%*	*Effect of increase*
0.040–0.040	3	2	50	Internal structure satisfactory. Shear value increased 10%	4	50	Internal cracks in welds. Shear strength unchanged
0.064–0.064	8	6	25	Internal structure satisfactory. Shear value increased 11%	10	25	Internal cracks in welds. Shear strength unchanged
0.102–0.102	8	7	12.5	Internal structure satisfactory. Shear value increased 5%	9	12.5	Internal cracks in welds. Shear strength unchanged

Slight reduction in Forge-Delay tends to reduce shear value slightly with no change in structure of weld, while small increases in forge time result in cracked welds.

Table 4C—Effect of Changing Welding Current (Max) When Welding 52S-H34

Gage thickness in.	*Schedule value amp*	*—Decreased value—* *Amp*	*%*	*Effect of decrease*	*—Increased value—* *Amp*	*%*	*Effect of decrease*
0.040–0.040	18,200	16,700	8	Internal defects in structure reduced 20%	20,000	10	Internal structure satisfactory. Shear increased 3%
0.064–0.064	26,400	24,600	7	Internal structure satisfactory. Shear reduced 27%	28,500	8	Internal structure satisfactory. Shear increased 11%
0.102–0.102	33,000	...	..	.....	...	..	.....

Increase in weld current is followed by increase in shear value with no apparent change in weld structure. Reduction in weld current may result in cracks or porosity in welds.

Table 4D—Effect of Changing Down-Slope Current When Welding 52S-H34

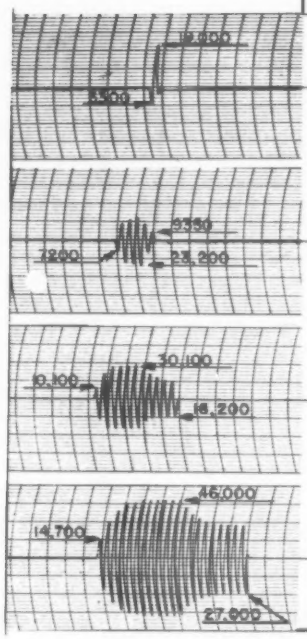
Gage thickness in.	*Schedule value amp*	*—Decreased value—* *Amp*	*%*	*Effect of decrease*	*—Increased value—* *Amp*	*%*	*Effect of increase*
0.040–0.040	9,600	9,000	6	No change	12,400 18,200*	30 90	No change. Internal structure satisfactory. Shear increases 10%
0.064–0.064	12,900	10,500	8	No change	26,400*	110	Internal structure satisfactory. Shear value increases 8%
0.102–0.102	27,700	24,400	12	No change. Internal cracks. No change in shear	33,000	14	No change

* Full maximum weld current.

Reduction in Down-Slope current by 16% may cause cracks. Down-Slope current may be raised to full welding current level with only small changes in shear and no defects.

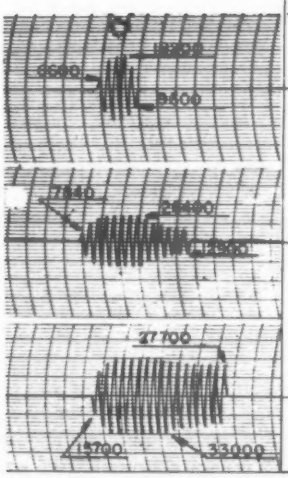
The type of weld schedule did have a marked effect on the number of welds between electrode cleanings when welding the 0.020–0.020 in. material. If a type of schedule such as used on 24S-T3 Alclad was used on 61S-T4 in 0.020–0.020 in. thickness; that is, using one cycle Up-Slope time and two-cycle weld, it was not possible to make satisfactory welds at all. Penetration was below 20% and often partial or "dud" welds resulted when metal expulsion between electrodes and sheets was avoided. If a one-cycle weld time, with no Up-Slope control is used, welds could be made of satisfactory qualification character. However, only five or six welds could be made before the electrodes had to be cleaned.

* If Up-Slope control* of one cycle was used and also a Weld time of one cycle, the effect was to have one half cycle of current much lower in magnitude than the next half cycle. The first half cycle tended to prepare the sheet surface and the second half cycle to do the actual welding. Using this type of schedule, welds were made of satisfactory qualification standards and it was possible to make 50 welds instead of 6 welds between electrode cleanings. This would obviously be considered a more satisfactory schedule for production purposes.



SHEET THICK	WELDER CONTROL SETTINGS: HEAT DIAL UP SLOPE	HEAT DIAL WELD	HEAT DIAL DOWN SLOPE	TIME UP SLOPE	TIME DOWN SLOPE DELAY	TIME DOWN SLOPE	TIME WELD
.020	20	96		1			1
.040	40	90	35	2	3	2	5
.064	40	92	50	3	5	5	11
.102	40	90	60	4	11	5	20

Fig. 6 Oscillograms of primary current. 61S-T4



SHEET THICK	WELDER CONTROL SETTINGS: HEAT DIAL UP SLOPE	HEAT DIAL WELD	HEAT DIAL DOWN SLOPE	TIME UP SLOPE	TIME DOWN SLOPE DELAY	TIME DOWN SLOPE	TIME WELD
.040	40	90	50	2	3	2	5
.064	40	90	50	3	8	5	14
.102	40	97	70	4	8	5	17

Fig. 7 Oscillograms of primary current. 52S-H34

SENSITIVITY OF WELDING SCHEDULES WHEN SPOT WELDING 61S-T4

The sensitivity of the various variables has been recorded in the form of tables for easier reading and analysis. These tables are numbered 3B, 3C, etc., indicating they all apply to the weld schedule as shown in Table 3.

It is emphasized that only one variable, that used as the heading for the table, is being changed. The other variables remain as indicated in Table 3.

As in the 24S-T3 work, the effect of the change is based on a sample of 3 to 10 welds for each change.

Forge-Delay time should be equal to Down-Slope Delay time or up to 20% later. Early application of forge results in a reduction of shear value. Forge pressure may not be necessary at all on the 0.020-in. material since the initial force is high enough to prevent cracks on this short-time weld.

Welding current should be maintained close to the values in Table 3 since slight increases may cause spits and slight decreases result in loss of shear value. The 0.040–0.040 in. and 0.064–0.064 in. schedule are more sensitive in this respect than the 0.102 in. schedule.

Up-Slope current may be raised and lowered over a considerable range without affecting weld quality or shear strength.

Down-Slope current may be raised as much as 22% with only a 14% increase in shear value but lowering by 20% may result in internal weld cracks.

SPOT WELDING 52S-H34 SURFACE PREPARATION

No difficulty was encountered in obtaining low uniform contact resistance on this material except that a black smut had to be wiped off in the final water rinse.

The procedure was to degrease, rinse 2 min in hot water 130–140° F, deoxidize, rinse 2 min in cold water and wash off black smut with a paper towel, dry by air blast.

* The initial heat or Up-Slope current was set at 20% and the weld current at 96% but the maximum current magnitude only indicated 90% of maximum was reached at the 1/4 cycle period.

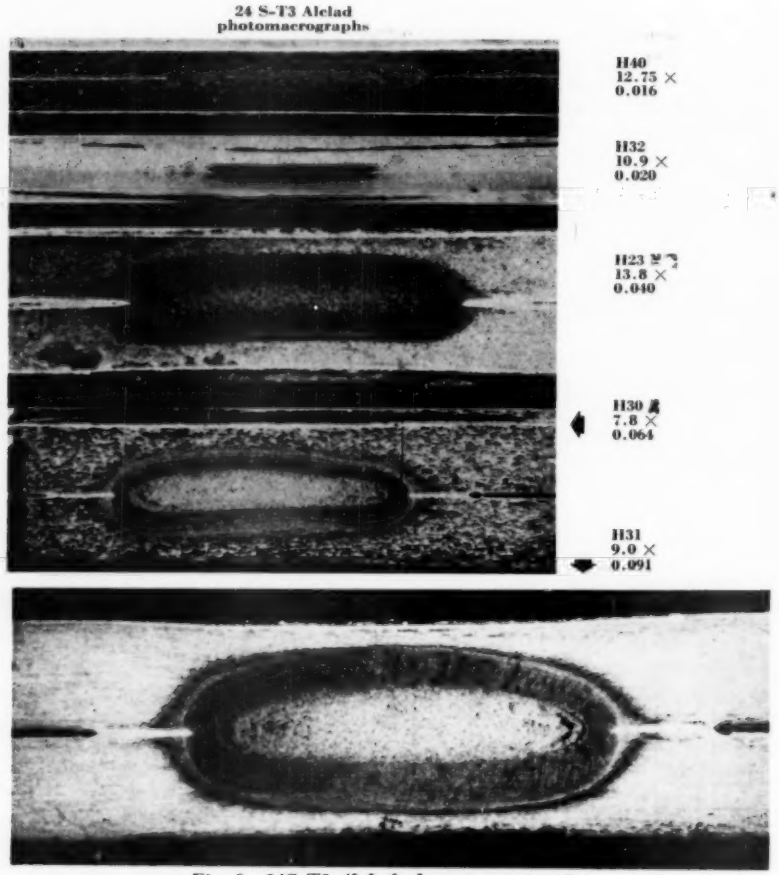


Fig. 8 24S-T3 Alclad photomacrographs

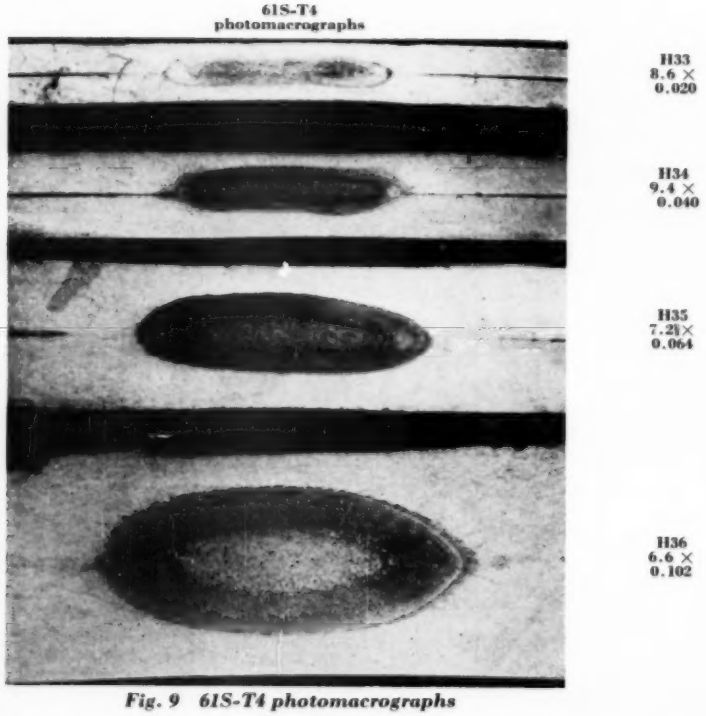


Fig. 9 61S-T4 photomacrographs

Table 6 shows the time and temperatures used in the cleaning.

The satisfactory welding schedules for three gages of this alloy are shown in Table 4 and the test results of these schedules are given on Table 4A.

The weld current patterns are shown in Fig. 7. Photomacrographs showing weld structure and penetration are included as Fig. 10.

Electrode Cleaning

On all schedules, at least 50 welds could be made before electrodes were cleaned and then only slight trace of aluminum was evident on the electrodes. No attempt was made to determine the actual number of spots which could be made without causing deviation beyond the limits of MIL-W-6860.

Eutectic stringers as shown in macrosection H38, Fig. 10, were quite evident on the 0.064-in. material. An effort was made to reduce their presence by using total weld time of 5 cycles at higher current. Such welds spit occasionally and stringers were worse. Sharper (less radius) electrodes were tried and condition again became worse with occasional spits. The nature of these stringers appears to be that they start at the interface and often grow through the sheet toward sheet surface. Their starting point at the faying surface is usually outside of the outer edges of the fused zone.

SENSITIVITY OF WELDING SCHEDULES WHEN WELDING 52S-H34 MATERIAL

The effects of changing one variable at a time from schedule shown in Table 4 are shown in Tables 4B and 4C, etc.

These variations are again based on 3 to 10 weld samples for each change. A short summary of each effect is given with each table.

The results of these changes are listed below: Forge delay time could be reduced one to two cycles with a reduction in shear of 5 to 11% and no effect on internal structure. When forge delay time was lengthened 1 to 2 cycles beyond the start of Down-Slope then internal cracks resulted without any increase in shear value.

Welding current could be increased 8 to 10% with small changes in shear value. When welding current is decreased by 8% internal defects may result in the welds.

Up-Slope current could be raised or lowered as much as 38% without apparent changes in weld structure as shear strength.

If Down-Slope current was reduced as much as 15% then cracks would appear in the welds. Down-Slope current could be raised to full welding current level without causing defects and shear strength was increased only 8 to 10%. This would indicate that controlled Down-Slope is not necessary if sufficient Weld time is used

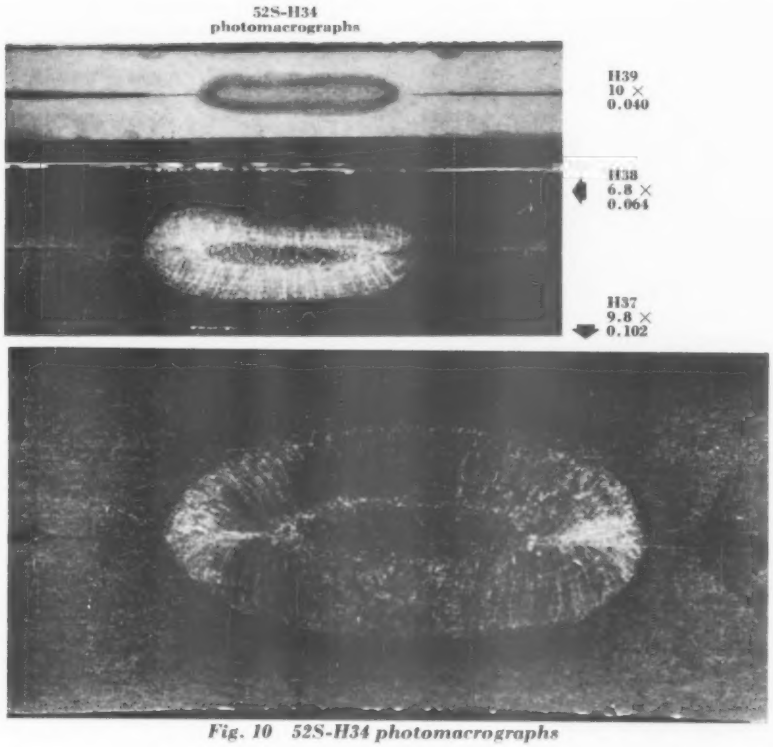


Fig. 10 52S-H34 photomacrographs

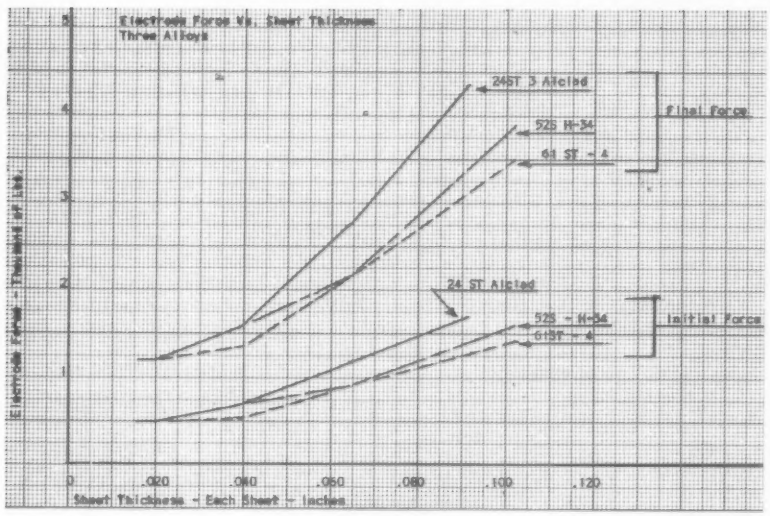


Figure 11

but this observation would have to be confirmed by full welding schedules without Down-Slope control.

A complete qualifying weld schedule was made at 1450 lb initial force and 3450 lb final force rather than 1600–3900 lb as listed in Table 4. Although this schedule was completely satisfactory in itself, it was very critical to small variations. A change in rate of Down-Slope or Down-Slope current level resulted in cracked welds. This schedule was critical in many other respects and shows the necessity of using the forces listed in Table 4.

Table 5—Chart of Ratios of Up-Slope and Down-Slope Current to Welding Current

Material; gage, in.	*Down-Slope current: weld current, %*	*Up-Slope current: weld current, %*
24S-T3ALC		
0.016	..	31.4
0.020	..	42.8
0.040	37	38.0
0.064	54	31.3
0.091	71	38.0
61S-T4		33.0
0.020	40.4	31.0
0.064	52.5	33.5
0.102	58	31.9
52S-H34		
0.040	53	36.2
0.064	49	29.7
0.102	84	47.5

SUMMARY

A set of graphs, Figs. 11 to 17, have been plotted from Tables 2, 3 and 4.

Figure 11 is a graph of electrode force versus sheet thickness for all three alloys. Figure 12 is a graph of welding current versus sheet thickness for all three alloys. Figure 13 is a graph of Up-Slope current versus sheet thickness for all three alloys. Figure 14 is a graph of Down-Slope current versus sheet thickness for all three alloys.

Figure 15 is a graph of Weld time, Down-Slope Delay time, Forge-Delay time, Down-Slope time and Up-Slope time versus sheet thickness for 24S-T3 Alclad.

Forge-Delay time and Down-Slope Delay time are identical and from 5 cycles down to 1 cycle Down-Slope time follows the same curve.

Figure 16 is a graph of time versus sheet thickness for 61S-T4. Here again Down-Slope Delay, Forge and Down-Slope all follow the same curve part of the way.

Figure 17 is a graph of time versus sheet thickness for 52S-H34 Down-Slope Delay and Forge Delay are identical but Down-Slope time is a separate curve.

Table 5 is a chart of the ratios of Up-Slope and Down-Slope current to welding current. This Table 5 shows that as the sheet thickness increases the ratio of

Table 4E

Thickness in.	*Degreaser type*	*Time min*	*Temperature ° F*	*Type*	*Time min*	*Temperature ° F*	*Contact resistance microhms*
0.040	Cowles AM	10	165	Weldal	5	190	6.3–18
0.064*	Cowles AM	10	165	Weldal	10	185	3.9–6.3
0.102	Cowles AM	10	165	Weldal	12	185	4.3–18.5

* Two sides of sheet not the same. One side etches slightly in degreaser while other side is unchanged. Evident difference in welding unless same sides were kept together at all times.

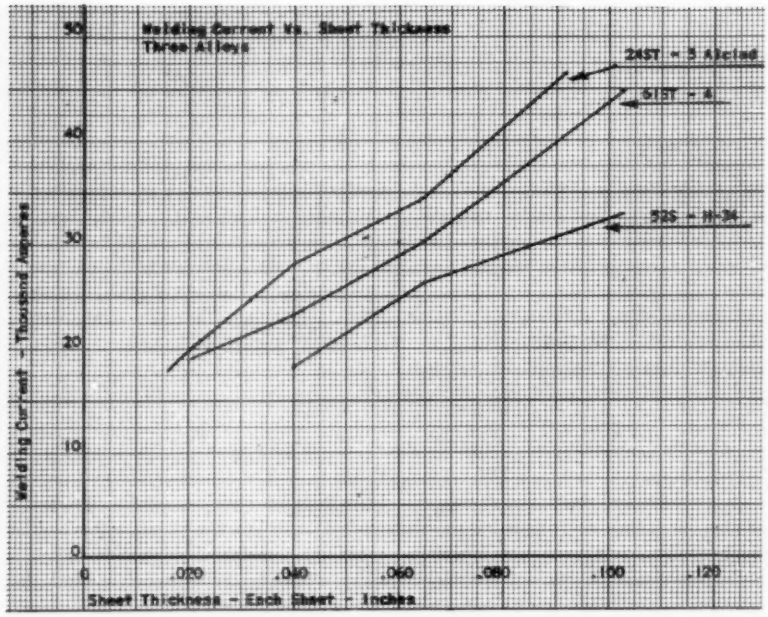

Welding Current Vs. Sheet Thickness
Three Alloys
24ST = 3 Alclad
61ST = 4
52S = H-34
Welding Current - Thousand Amperes
Sheet Thickness - Each Sheet - Inches

Figure 12

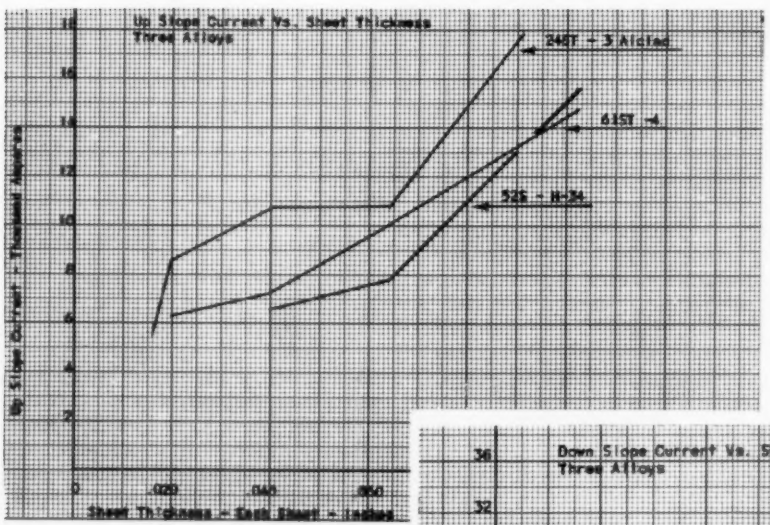

Up Slope Current Vs. Sheet Thickness
Three Alloys
24ST = 3 Alclad
61ST = 4
52S = H-34
Up Slope Current - Thousand Amperes
Sheet Thickness - Each Sheet - Inches

Figure 13

Down-Slope current to welding current must increase to make up for heat losses in the sheet. The ratio of Up-Slope current to weld current stays relatively constant.

Photomacrographs of sections H24 and H31 (Fig. 8) and H38 (Fig. 10) all show a series of concentric rings in the outer cast structure of the weld nugget. These rings have never been noted on direct current welds and appear to be caused by the change in cooling rate as the Down-Slope current passes through zero each half cycle.

CONCLUSION

This investigation has shown that it is perfectly feasible to make welds which are superior to the requirements of MIL-W-6860 on a-c single-phase equipment with Up- and Down-Slope current control and forging pressure. To support this, a group of qualifying weld schedules each producing radiographically crack-free welds, with an average shear value of 9 to 63% above specification lowest average shear, have been presented.

In order to show the sensitivity of each schedule, variation in weld current, Up-Slope current, Down-Slope current and Forge Delay times have been made.

This schedule information has been presented in three sections; one for each alloy welded.

It has been shown that electrode cleaning is not a serious problem once low uniform contact resistance is obtained. If contact resistance is not low and uniform then trouble from pickup and nonuniform shear values can be expected.

A method of using Up-Slope current control to an advantage on the lighter gages has been discussed. Two supporting schedules using this method are included in the qualifying schedules.

Appendix

PRECAUTIONS TO BE CONSIDERED WHEN APPLYING SINGLE-PHASE RESISTANCE-WELDING EQUIPMENT FOR THE SPOT WELDING OF ALUMINUM ALLOYS

The general type of single-phase equipment consisting of welding machine and control is offered by several manufacturers. Briefly, the welder should be equipped with a movable head assembly possessing low and constant friction and inertia and have means of applying a forging pressure rapidly after the double pres-

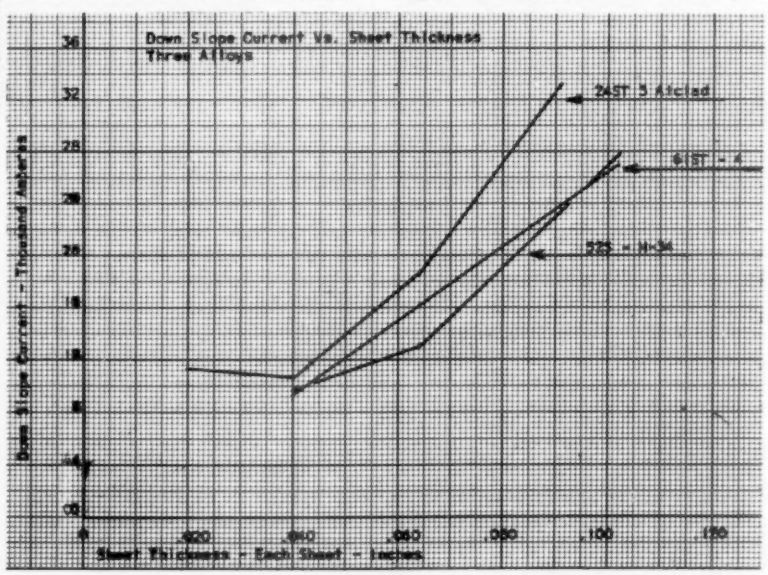
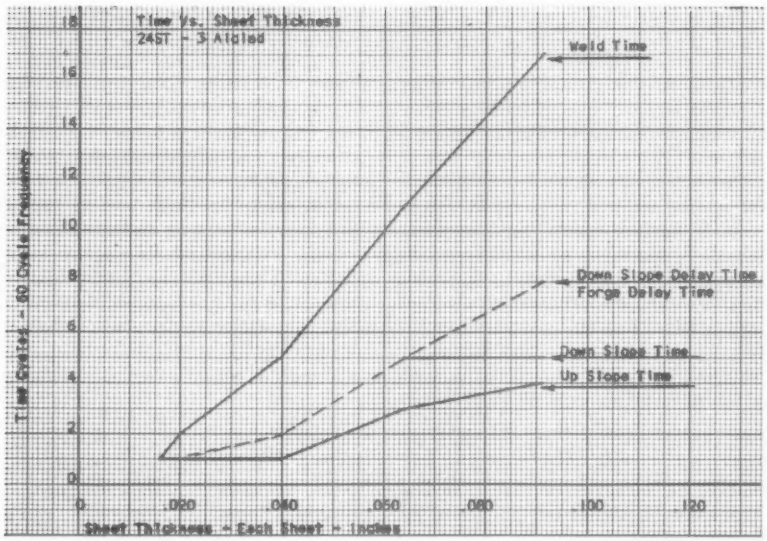

Down Slope Current Vs. Sheet Thickness
Three Alloys
24ST 3 Alclad
61ST = 4
52S = H-34
Down Slope Current - Thousand Amperes
Sheet Thickness - Each Sheet - Inches

Figure 14


Time Vs. Sheet Thickness
24ST - 3 Alclad
Weld Time
Down Slope Delay Time
Forge Delay Time
Down Slope Time
Up Slope Time
Time Cycles - 60 Cycle Frequency
Sheet Thickness - Each Sheet - Inches

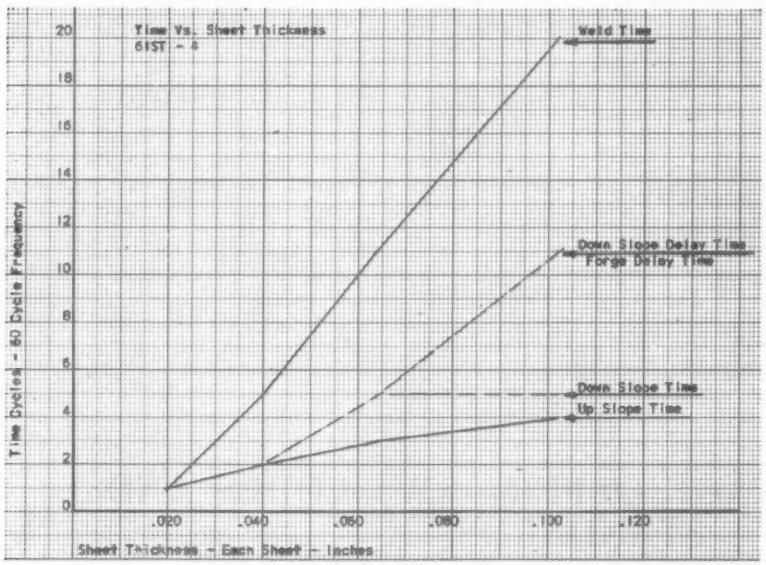


Figure 16

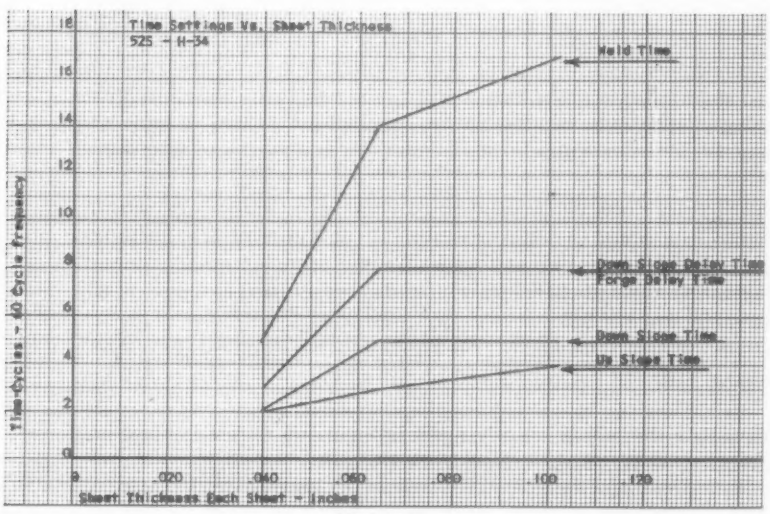


Figure 17

Figure 15

sure system is actuated. The welder must have sufficient transformer capacity to supply the necessary welding current at the desired duty cycle. The control equipment must have precision timed circuits for Down-Slope delay or Weld-Heat time, for Forge Delay time, for Up-Slope time and Weld time. Independent adjustment of initial and final heat levels is also required.

One of the most serious problems often initially overlooked by the potential user is the large electrical load involved in this type of welding. On the average 36-in. throat spot welder of 150 kva capacity, the anticipated single-phase kva demand when welding 0.091 in. thick 24S-T3 Alclad (based on 47,000 amp welding current) is approximately 600 kva at a power factor of approximately 22%. This is almost seven times the kva demand expected when welding a similar thickness of low-carbon steel.

As the kva demand is appreciable, the power distribution transformer supplying the welder must have a kva rating considerably larger than the welding transformer. As an example, in the above case of 600 kva demand, if the voltage drop in the distribution transformer is to be limited to 5%, the kva rating of this transformer would be 600 kva assuming a 5% internal impedance. If the internal impedance is 3% the kva rating would still be approximately 350 kva.

It is recommended that the local utility be advised of the single-phase kva demand so they can judge intelligently the size of distribution transformer required and whatever other disturbance such as load may cause before purchasing welding equipment.

The example indicated above was based on a 36-in. throat depth spot welder. Appreciably lowering of the kva demand requirements results if an 18-in. throat depth welder is used. Under the aforementioned conditions, the 18-in. throat depth welder would take 450 kva demand compared to 600 kva demand for the 36-in. throat depth.

Unless transformer capacity is available it is wise to investigate the use of three-phase equipment (initially more expensive) which has two inherent beneficial effects to reduce the kva demand during welding. The first is the actual reduction in total kva demand to approximately half of that of the single-phase load. The second is the resultant lower load which is also distributed over the three phases. This combined effect reduces the load from 600 kva single-phase to approximately 175 kva per line. Power factor is also greatly increased.

One other serious factor is noticeable in production which would not be apparent from the work in this report. Specifica-

tion MIL-W-6860 allows the changing of one adjustment 10% without the necessity of recertifying the welding schedule. For a specific geometrical welder throat configuration, this limit is satisfactory.

Let it be assumed that a certification schedule has been made for one operation on a product made up of two equal thicknesses, such as 0.064–0.064-in. 24S-T3 Alclad. This operation is performed using a specific welding throat geometry and a recording of welding schedule adjustments are made which identifies the certified welding schedule. However, if the next operation requires a different throat area because of the necessary electrode set up to be used, the aforementioned limit of 10% adjustment, especially on weld current, may have to be exceeded in order to make the necessary welding current flow through the different throat impedance caused by changing the throat area. If the 10% adjustment has been exceeded to make the proper welding current flow, it is necessary to make a new certification schedule. This recertification adds a tremendous amount of time, cost and effort to the welding operation as certification schedules need not only be run for different materials and thickness combinations but also on almost every operation involved.

Only seemingly minor adjustments in throat depth or horn clearance cause a 10% change in impedance. If a 36-in. throat depth welder with an 8-in. horn spacing is considered as a base, a 10% change in impedance occurs if the throat depth is changed approximately 4 in. or the horn clearance is changed approximately $2^1/_2$ in.

The additional certifications result from the wording rather than the intent of specification MIL-W-6860. The specification states control on an adjustment with the intention of that variable not exceeding 10% from the certification value. If the specification called for tolerance on the variable directly, which would have to be measured by suitable means, the value could be adjusted to the mean value to take care of differences in throat impedance and the 10% adjustment apply to the new setting. This would avoid recertification as, actually, the same welding schedule is being used but a different set of adjustments is necessary to give the same welding schedule values.

The above difficulty is not apparent on three-phase equipment in the same degree since the influence of throat area on the magnitude of welding current is of much less consequence.

Another factor to be considered is the range of adjustment of transformer secondary voltage. The secondary voltage should be adjusted by tap switch to allow the adjustment of welding current to fall between 90 and 100% weld heat dial setting for each gage to be welded. Failure to allow operation between 90 and 100% heat will seriously distort the welding current wave shape. Both Up-Slope and Down-Slope become decreasingly effective as the maximum heat is reduced by phase shift.

The use of the large welding currents necessary for welding aluminum produce intense magnetic fields in the welder throat. These fields can be so intense that if a section of the aluminum of cylindrical shape (forming a closed circuit) is inserted over one horn, the resultant eddy currents which are generated, react with the magnetic field and cause the cylinder to vibrate or shake. This shaking has the effect on the weld of electrode skid and makes it difficult to make round, sound welds necessary for MIL-W-6860 specification welding.

The above effect is not noticeable on three-phase equipment as the frequency of the secondary current is much lower or nonexistent.

ACKNOWLEDGMENT

The author wishes to acknowledge, with appreciation, the guidance and assistance received from fellow employees in the welding program and preparation of this paper.

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Improved Semi-automatic Welding and Hard Facing

A device for semiautomatic welding that combines the advantages of automatic welding with the flexibility of manual welding

by Howard S. Avery, Turner G. Brashear, Jr., Henry J. Chapin and Gerald H. Edmunds

Abstract

A new device that combines the advantages of automatic welding with the visibility and flexibility of manual welding is described. Electrode wire from coils is fed continuously, reaching the arc with a flux covering that can provide arc shielding, deoxidation, arc stabilization, slag coverage of the molten bead; and alloying if necessary. The operator can weld steadily for 15 min if desired; the delay of replacing electrodes and the waste of stub ends are eliminated. The concealment of the arc and the nuisance and expense of handling considerable volumes of unfused flux that characterize submerged arc welding are avoided.

The range of available welding currents and burn-off rates is wide. An automatic control of arc length makes the device easy to operate. The chief limitation of the device is the adaptability only to downhand welding, in the same way that submerged arc welding is restricted.

The possible range of filler metals is also wide. Fluxes that will deposit mild steel of the E60 series type, higher yield strength build-up steels, martensitic steels for wear and impact resistance, martensitic irons for abrasion resistance and austenitic manganese steel with the same roll of electrode wire have been studied. Under favorable conditions it requires only about one or two minutes to change from one type to the other, and no waste of flux or electrode is involved.

EVER since the use of covered electrodes became established, the intermittent nature and the high labor costs of manual arc welding have been matters of concern. This logically led to the development of automatic welding, which depended for its success upon protection of the molten weld metal by a blanket of granular flux, by a shield of inert gas or by inclusion of deoxidizing elements in the electrode. These three procedures are industrially important today and adaptations to hand or semiautomatic welding have been made. However, further improvements in cost or convenience are desirable.

Current manual semiautomatic welding under a flux blanket has some of the advantages of fully automatic operation in speed and high deposition rates, but concealment of the arc is a disadvantage in common. This limits the precision with which the weld deposit can be placed without jigs and fixtures. Further, the handling of the granular blanketing flux is a nuisance and expense. With good practice the amount of flux used may be perhaps twice the weight of wire consumed. To the cost of wire and flux must be added the labor involved in clean-up and salvage of the infused granules and sometimes of reclaiming the fused flux by grinding.

This paper describes the apparatus, procedures and materials that combine the advantages of automatic and manual welding. The process depends on a granular ferromagnetic flux that is held on the electrode by the magnetic field resulting from the flow of the welding current through the electrode. Thus the electrode wire, which is fed continuously from a coil, reaches the arc with a flux covering that contributes most of the advantages of coated manual electrodes.

The process and the materials are new in the United States, and since industrial use here is just beginning, this paper is in the nature of a progress report. The magnetic flux method was described[1] in Europe some years ago and during 1953 was adapted to a simple semiautomatic unit by T. G. Brashear, Jr., while with the Leader Welding and Manufacturing Co. The use abroad was apparently confined to relatively cumbersome automatic equipment and mild steel welding.

Various aspects of the process and the equipment are covered by existing patents and pending patent applications, while additional applications are in preparation. The magnetic fluxes used in the process reported here were developed in the Metallurgical Laboratory of the American Brake Shoe Co.

THE APPARATUS

The MF unit is essentially a wire feeding device and is thus related to various familiar automatic and semiautomatic machines. It consists of a chassis upon which are mounted a reel for the coiled electrode

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Fig. 1 Amsco magnetic flux welder

This unit is complete except for a source of welding power, which can be either a c or d c in the range from 200 to 400 amp. It also is adapted to manual submerged-arc welding.

wire, driving and guide rolls to feed the wire into a flexible connecting tube, a universal (a c and d c) motor that drives these rolls through a gear train, a relay that starts the motor when welding current flows and various electrical controls. The chassis is provided with wheels for easy movement and is pear shaped to provide some streamlining advantage when being pulled around the job. The total weight without the tube and hopper assembly is 52 lb and the unit as shown in Fig. 1 is 29 in. long, 19 in. wide and 13 in. high. For added convenience in some applications the unit can be hung on a wall by a simple bracket or mounted in a cabinet that protects the apparatus and permits adjustments without stooping to the floor.

The wire is fed through a $14^1/_2$-ft tube to a flux hopper with a welding nozzle and emerges through the nozzle orifice to the arc. An internal guide centers the wire in the detachable orifice, whose diameter controls the wire to flux ratio during welding. The hopper holds about a quart of flux (3–4 lb) and permits welding steadily for about 15 min without breaking the arc.

The MF unit is connected by a No. 4/0 copper wire cable to a conventional d-c or a-c welding generator. Using $^5/_{64}$-in. diam wire, currents in the range from 150 to 450 amp are appropriate. When the arc is struck, the welding current energizes the relay that connects the drive motor to the source of welding current and wire is then fed to the arc. The motor voltage, and consequently its speed, is controlled by the voltage drop across the arc. A variable resistor in series with the motor provides additional control. (Fig. 2.)

Lengthening the arc tends to increase its voltage, speed the motor and feed wire faster. The faster feed tends to shorten the arc. The faster burn-off rate of the short arc tends to lengthen the gap again. These opposing forces reach a balance point and hold the arc gap steady at a length determined by the rheostat setting in the motor circuit. The practically constant arc length makes it possible for the operator to concentrate on accurate weld metal placement and is a great help in training inexperienced welders.

In a test of its effectiveness a novice who had never

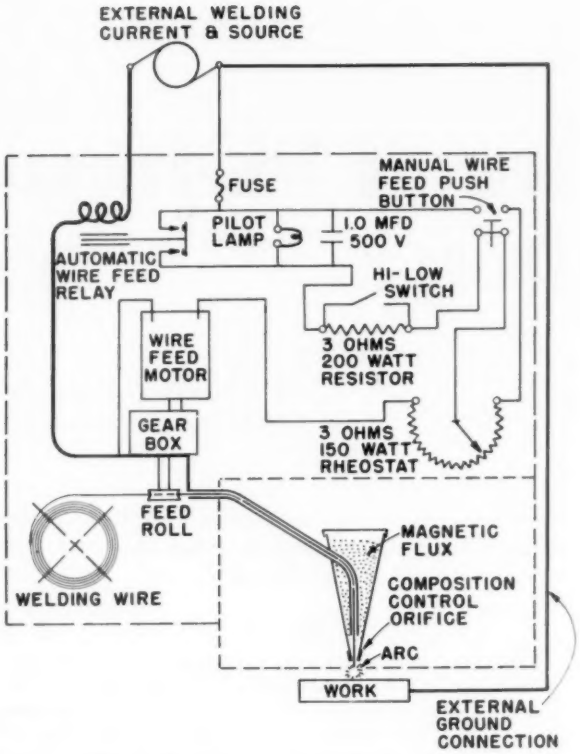

Fig. 2 Schematic wiring diagram of the magnetic flux welder

handled welding equipment, after five minutes of instruction, was welding satisfactorily, if not skillfully. The steadiness of the arc, as evidenced by recording meters, was comparable to that of an experienced welder with a manual electrode. (This does not imply that the equipment can substitute for the skill of a good welder in placement of weld metal.)

Familiar examples of automatic and semiautomatic welders that control arc length through arc voltage usually employ some electrical or mechanical means to magnify the voltage changes. This is not necessary with the MF unit, which permits a very desirable simplicity.

To feed wire without striking the arc, an independent circuit is used; the open-circuit voltage of the welding power supply, reduced by extra resistance, is applied to the motor by a push button switch.

The device as described is quite similar to several familiar devices that use nonmagnetic fluxes. With a large nozzle, a nonmagnetic flux will pour out rapidly and the unit can be used thus for manual semiautomatic submerged arc welding. With a quart of flux (about 60 cu in.) used thus, welding can continue for about one or two minutes. The arc length control makes this procedure quite practicable. However, large volumes of flux are involved, the slag from the fused portion must be separated from the unfused granules, and flux handling thus becomes burdensome. The weld bead and the arc are concealed by the flux blanket and

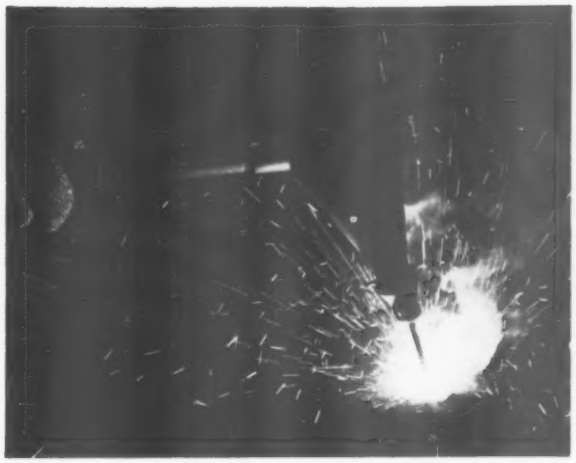

Fig. 3 Normal MF welding with about 3 in. of flux covered wire projecting from the hopper assembly

Fig. 4 To illustrate automatic arc control, operator has pulled away from work here. Note that feed control maintains correct arc length even with excessive amount of wire sticking out

precise positioning of weld metal is, therefore, difficult. If jigs or fixed supports are used to maintain electrode position, the flexibility of manual welding is lost. However, the MF unit can be easily converted to an automatic welding head for either magnetic flux or submerged arc welding if jigs are available.

The use of magnetic flux and a magnetic gate on the hopper nozzle are departures from familiar practice and open up wide areas for more efficient manual welding. The magnetic gate consists of several tiny permanent magnets in the walls of the nozzle orifice. The magnetic flux powder clings to these in tufts and thereby blocks the annular opening around the electrode wire. However, the magnetic field of the electrode, when passing the welding current, is so much stronger that it drags the flux along with the wire as it passes this gate. The outside diameter of the magnetized flux coating is controlled by the orifice diameter. The amount of flux (by weight) is further influenced by density and granule size. These variables can be controlled to influence various aspects of the process.

The control orifice cap is light and inexpensive. A bayonet fastening permits quick changes. The orifice can be readily drilled to any size desired or stocked with standard openings, and costs so little that any wear or damage to it can be rectified by prompt replacement.

The hopper assembly with its supply of flux is balanced to handle like the usual electrode holder. It is heavier than the latter and thus can be held somewhat steadier. The wire feed tube, which also carries the power cable, is slightly heavier than the usual cable and for continuous welding jobs should be supported as a convenience to the operator. As with ordinary manual welding the unbalanced load of the cable may cause fatigue of the welder's wrist muscles.

When set up for welding, several inches of bare wire will project from the hopper nozzle. The moment the arc is struck, the wire will feed into the arc and welding can proceed. However, without benefit of the flux, the arc is erratic and unstable, while the deposited metal is porous and of poor quality. With magnetic flux in the hopper, it will appear on the wire within a second or two and provide deoxidation and slag protection to the weld pool as well as arc stability. A pinch of flux placed where the weld is to begin will make starting easier and will compensate for the $^1/_{10}$ oz (or less) of weld bead made by the bare wire.

Once the flux-covered wire is feeding smoothly to the arc, welding is surprisingly easy, and control of the weld deposit and arc length is not dependent on acquired skill in feeding the electrode. For a few minutes at first a welder accustomed to stick electrodes will tend to feed the hopper assembly to the work. This will not interfere with arc action, but should not be carried to the point of permitting the arc to overheat or fuse the nozzle. On the other hand, a novice will tend to pull away from the arc, which the control factors will keep burning even if a foot of wire is projecting from the nozzle. The best working distance is about two or three inches from the arc to the nozzle. (Fig. 3.)

The nozzle orifice controls the proportion of flux to wire, and, therefore, the volume of slag. For the variety of jobs on mild steel, a series of orifices are provided that give the welder a choice of slag volume. This is an advantage not provided by stick electrodes unless both lightly and heavily coated types of the same grade are at hand. For mild steel the smallest orifice that will give an adequate slag cover is usually selected. Too little slag will expose the weld metal to excessive oxidation and will result in porosity and poor bead contours.

For manganese steel and hard-facing fluxes the proportion of flux to wire is determined by manufacturing research and should be maintained carefully during use. Table 1 will show how orifice size affects weld metal composition. However, the different orifices permit the user to experiment with composition if he wishes to do so.

Table 1—Manganese Steel Compositons from MF Welding

Flux No.	Orifice diam, in.	Wire diam, in.	Wire:flux Ratio	Welding current, amp	Weld deposit composition, % C	Mn	Mo
MF52	3/16	5/64	1.85	200R	0.15	2.58	0.77
MF69	9/32	3/32	1.72	200R	0.14	3.55	1.00
MF69	9/32	5/64	0.73	200R	0.71	11.74	1.60
MF69	9/32	1/16	0.63	200R	0.77	12.01	1.69
MF69	5/16	5/64	..	...	0.55	6.95	1.78[a]
MF69	5/16	5/64	..	...	0.71	13.28	1.48[b]
MF69B	9/32	5/64	0.80	200R	0.47	5.96	1.82[a]
MF69B	9/32	5/64	0.61	200R	0.72	12.76	1.82[b]
MF84	9/32	5/64	0.44	200R	0.64	6.0	2.61
MF85	9/32	5/64	0.64	200R	0.65	8.4	2.45
MF125	9/32	5/64	0.70	200R	0.64	7.56	2.54
MF181	5/16	5/64	0.54	200R	0.61	11.72	1.44
MF283	9/32	5/64	1.27	200R	0.43	12.40	Pnd
MF284	9/32	5/64	0.99	200R	0.63	14.90	Pnd
MF285	9/32	5/64	1.10	200R	0.60	13.85	Pnd
MF290	5/16	5/64	..	300R	1.02	17.42	0.98[c]
MF290	5/16	5/64	..	300R	1.07	17.42	0.93[d]
MF290	7/32	5/64	..	185R	0.76	13.36	0.78
MF290	7/32	5/64	..	185R	0.61	12.35	Pnd
MF294	9/32	5/64	0.88	200R	0.77	12.44	Pnd
MF338	5/16	5/64	..	300R	1.06	13.75	0.87[e]
MF338	5/16	5/64	..	300R	1.05	13.72	0.94[f]
MF338	9/32	5/64	0.83	200R	1.20	18.13	Pnd
MF338	7/32	5/64	1.02	200R	0.94	15.38	1.03
MF346	9/32	5/64	0.75	200R	1.12	17.43	1.30
MF346	7/32	5/64	0.97	200R	0.84	14.13	1.04

[a] Coarse flux granules. [b] Fine flux granules. Pnd = present but not determined.
[c] Bhn 207. [d] Bhn 193. [e] Bhn 211. [f] Bhn 219.

Each time the arc is broken the operator should inspect the nozzle and brush it clean of spatter accumulation. If this is neglected and the nozzle is held too close to the arc, the spatter build-up may reduce the effective orifice size and interfere with flux flow.

The electrode size characteristics of MF welding are quite different from stick electrodes. Neither the wire diameter nor the diameter of the covering (established by the nozzle orifice) correspond to conventional electrodes. The 5/64-in. wire that is standardized for convenience will serve the range from 150 to 600 amp. Some relations between electrical values and deposition rates for mild steel appear in Fig. 5.

It will be recognized that MF welding provides a marked advantage in deposition speed. Exploitation of this will depend on maintaining satisfactory weld deposit character and an avoidance of base metal damage in the heat-affected zone. The high and continuous heat input of high current MF welding should be avoided with manganese steel, but are desirable for mild steel if the welds are sound. However, even when welding with low currents that give the same deposition rates as manual electrodes, many advantages appear.

Stub end loss, representing a waste of from 10 to 25% or more with conventional electrodes, is eliminated. Electrode changing is not necessary and high-duty cycles are made feasible.

In a plant test involving 6 days of welding, the arcs of MF units were burning 60% of the time on simple work. A 30% cycle is frequently used for estimating welding time requirements and thus the MF process promises improvement.

Since long fillet welds can be made without arc interruption for changing electrodes the trouble of filling craters and the recurrent liability of crater cracks are minimized.

The deposition efficiency of the wire and flux combination has ranged around 60% for mild steel, using an orifice to give about 2:1 for a wire to flux ratio. About 20% goes into the slag and perhaps 20% is lost as spatter, smoke, etc.

THE ELECTRODE WIRE

The various sizes and types of welding wire appropriate to submerged melt and sigma welding can be used. To simplify the pattern 5/64-in. diam mild steel wire has been standardized for most of the MF welding. This meets the specification of

C, %, max 0.10
Mn, % 0.25–0.50
P, %, max 0.04
S, %, max 0.05

It is desirable that the coiled wire be copper coated to minimize rust during storage and exposure to the air before use. The reel on the MF unit accommodates layer-wound coils 12 in. ID and 2 1/2 in. wide.

For some hard facing and build-up applications it is advisable to use a high carbon wire, such as the AISI 1065 grade.

THE MAGNETIC FLUX

Electrode coatings and submerged melt fluxes consist largely of various oxides and minerals that form slag at the moment of welding. To these, ferromagnetic substances such as ferrite, nickel or magnetite, must be bonded to make magnetic granules that will

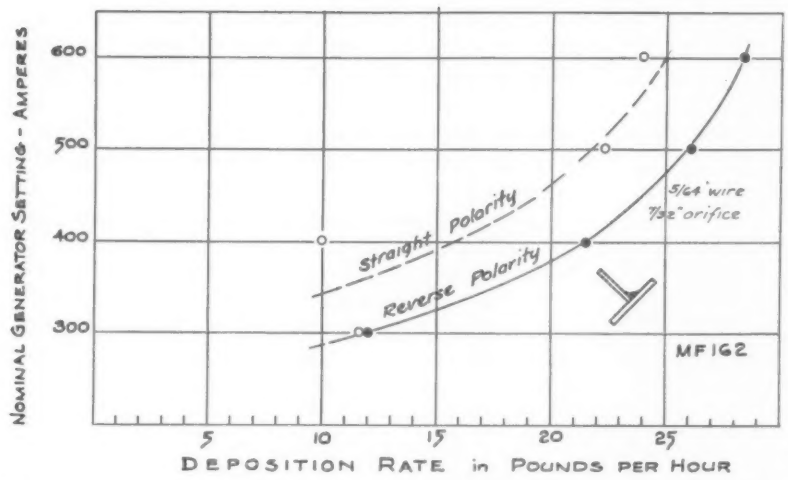

Fig. 5 Preliminary survey of deposition rates made with mild steel magnetic flux in downhand fillet welding

cling to the electrode wire when it is passing the welding current. The manufacture of the flux involves many of the same difficulties that occur with coated manual electrodes, but other problems are peculiar to the magnetic flux. The magnetic permeability of the granules must be controlled, the granule size range and the distribution within this range are important and relation between orifice size, granule specification and flux type must be carefully worked out by research and accurately controlled in commercial production.

The flux must flow freely in the hopper and through the nozzle. As manufactured the magnetic fluxes will do this but they must be kept dry. Since bad effects of damp electrodes are known to most welders, this requirement is easily understood. Damp flux not only will flow poorly, but it is likely to cause porous deposits.

The physical character of the magnetic electrode coating introduces several problems. Good extruded coatings cling tightly to the wire until they melt and operate to exclude air from the weld metal. The magnetic flux entrains some air and brings it into the arc zone. This can set the stage for weld porosity unless the trend is counteracted metallurgically. Also, the flux particles, being light and held only by magnetic force can be thrown around by arc action. As long as they remain magnetic, they will try to follow the high current density portion of the arc core, but they are heated into the nonmagnetic temperature range so quickly by the arc that this effect is very brief. The end result is a marked tendency to spatter. This can be controlled practically by careful flux formulation, but even at best the process will probably exhibit more fine spatter than the best manual electrodes. This feature is not serious, since the spattered material is readily brushed off and is thus distinguished from the molten metal that is thrown around by vigorous reactions in the weld pool.

Extensive laboratory experiments have been made with several hundred magnetic fluxes, but no attempt will be made to report this here. However, certain types that probably will reach commercial production are worthy of a brief description.

AUSTENITIC MANGANESE STEEL

Manganese steel electrodes, whose technical features have been described in a previous paper,[2] are widely used to build up worn portions of massive manganese steel parts and to provide wear-resistant overlays. Weld metal for this purpose should be of a modified Hadfield composition that will achieve good toughness without water quenching,[3] that will have good yield strength to resist deformation from impact and that will be resistant to cracking as the weld cools. Peening is recommended to relieve the cooling tensile stresses from manual welding, but for MF use the weld should be satisfactory without this. These requirements, some of which are in opposition, make formulation difficult.

The range of manganese steel composition attainable is illustrated by Table 1, which also shows the influence of orifice size on composition. The wire used was mild steel (0.10% C); welding was d c, reverse polarity.

The relation of welding and formula variables to deposit composition was surveyed rather carefully since the toughness and other mechanical properties of this material are closely related to composition. The martensitic steels below 8% manganese can be detected because they are magnetic, and their weld deposits are likely to be above 220 Bhn, but neither magnetism nor Brinell hardness serve to detect the inferior austenitic compositions with assurance. If manganese is between 10 and 14% and carbon between 0.50 and 1.0% the unalloyed compositions will be nonmagnetic and will serve some needs, but for best properties the presence of molybdenum or nickel is advisable.[2]

It should be appreciated that the ranges in Table 1 are likely only with careless or inept handling of formula and welding variables. The commercial fluxes resulting from this research program are subjected to quality control tests to ensure proper deposit composition with the specified orifice size. This manganese flux is controlled to attain all weld metal tensile properties in the neighborhood of 62,000 psi minimum yield strength, 100,000 psi minimum ultimate tensile strength and above 18% elongation.

The slags on manganese steel MF deposits are usually quite easily removed. Some of them can be lifted off in one piece. Their character can be changed by processing variables and there is a temptation to make them thick and heavy to get very easy removal. However, welds with such slags are subject to suspicion because they are likely to be low in manganese content.

Welding on manganese steel should be as cool as practicable to avoid embrittlement of the base metal. Currents above 200 amp should generally be avoided. This prevents exploiting the high deposition rates that MF welding makes possible, but it will give more satisfactory structures and will minimize cracking, which is related to arc power and bead size[3, 4]

HIGH-STRENGTH BUILD-UP TYPE

This flux, when used with a 0.10% carbon steel wire will deposit weld metal with the structure of low-carbon martensite. With a $^{7}/_{32}$-in. diam orifice the weld will have a hardness of from 250 to 400 Brinell. It will be quite tough and may be expected to have a yield strength of above 85,000 psi. The alloy balance of the flux is such that it can also be used with a 0.65% carbon steel wire and thus produce a higher weld hardness.

The use of 0.65% carbon steel, without alloying, is quite common for build-up purposes. It can be used thus with the mild steel flux to obtain a weld with a Brinell hardness of above 150, and a pearlitic structure. This will not be so strong and tough as the metal from the flux formulated for build-up use, but for some purposes it will be satisfactory. It will respond to heat treatment in the way that a shallow hardening carbon steel does.

HARD-FACING FLUX

The inclusion of alloying elements in the flux makes possible hard-facing deposits. The alloy should be high enough to produce a martensitic structure as the weld cools. The carbon level should be matched to service requirements of toughness.

A number of fluxes that will give hard deposits over 500 Bhn have been studied. Here again the balancing of orifice size, welding currents and wire to flux ratio is important if consistent results are to be obtained. Both the steel and martensitic iron carbon ranges are attainable. However, so far it has not appeared feasible to deposit the cobalt base alloys or the composite tungsten carbide types by this method.

The use of more than one kind of wire with a given hard-facing flux and the possibility of mixing fluxes present users with a multitude of deposit compositions. Undoubtedly some will wish to experiment. The level of hardness attained, and by inference the approximate toughness, can be varied readily and checked without difficulty. The resulting structures, which control other properties to a large extent, will require metallographic equipment for study. The behavior under abrasion and other specialized wear conditions is much more difficult to evaluate, and for best results it probably will be advisable to use wire and flux combinations that are established by laboratory research. Such work is in progress.

MILD STEEL WELDING

It seems likely that the fabrication of mild steel will be the most useful field for MF welding. It lends itself well to horizontal and downhand work. In many cases the use of the manganese steel, build-up and hard-facing fluxes will be incidental to the possession of a unit for general welding.

The primary reasons for mild steel MF welding will be speed and economy. For this reason it will probably compete with the existing semiautomatic submerged flux welding and with the powdered metal electrodes.

Table 2 indicates the properties observed on a series of all weld metal tensile specimens. These were made as V-butt welds in $^{3}/_{4}$-in. thick plate, following the practice prescribed in AWS specification A5.1-48T. (ASTM Designation A233-48T.)

The process lends itself well to horizontal fillet welds, which can be made at high rates. The quality of such welds has been good with moderate deposition rates. As currents and deposition rates increase there is a trend for weld porosity to increase. How far the speeds can practicably be extended upward has not yet been fully determined. Figure 4 shows data obtained during welding. It can be seen that welding currents above 300 amp promise attractive economies.

Table 2—Properties of All-Weld Metal Tensile Specimens; Deposits with Mild Steel Magnetic Flux

Yield strength, 0.2 set, psi	*Ultimate tensile strength, psi*	*Elongation, % in 2 in.*	*Reduction area*	*Brinell hardness*
56,400	70,500	16.0	25	149
61,200	72,200	16.0	20	...
59,400	78,100	18.5	20	174
57,600	74,900	21.0	24	170
54,000	72,100	19.0	26	156
56,400	74,000	19.5	27	192
55,700	74,800	16.5	17	174
53,900	70,100	22.5	26	170

The metal of single-pass welds is quite different from that in tensile specimens as shown in Table 2. The tensile test welds required many passes, the heat of which operates to recrystallize and refine the grain size of preceding passes. A single pass is coarse grained and comparatively brittle. This is not a limitation of MF welding alone; it applies to manual and automatic welding as well. This is recognized by those who write filler metal specifications. It is repeated here for the benefit of any who may be tempted to design fillets on the basis of the data in Table 2.

SUMMARY

The magnetic flux semiautomatic process appears to offer the following advantages:

1. The speed and convenience of submerged melt technique but with the visibility and flexibility of manual welding.
2. Adaptability to a variety of filler metals, including mild steel, high yield strength steels, manganese steel and hard-facing alloys.
3. Quick change over from one type to the next.
4. Good deposition efficiency versus submerged melt welding.

5. Minimizing the weld craters.

6. Elimination of electrode stub ends.

7. Easy instruction in holding an arc and quick learning for novices.

8. Simple, portable equipment.

Its disadvantages are not yet clarified but probably include:

1. Sensitivity to orifice size selection.

2. Greater spatter than from manual electrodes.

3. Limitation to flat, horizontal welding. Vertical welding probably is feasible, but the process is not now adaptable to overhead welding.

4. Tendencies toward porosity at high current levels.

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Shielded-Inert-Gas Metal-Arc Welding of Carbon Steel

Application data for successful procedures on the shielded-inert-gas metal-arc welding of carbon steels

by J. R. Craig

METAL arc welding within a shield of inert gas, a relatively new fusion welding process, was first found to be a useful and economical production tool for the joining of nonferrous metals. Initial attempts to weld carbon steel using inert gases or gas mixtures then known produced deposits of very poor quality. After considerable research, it was found that satisfactory weld deposits could be made at a reasonable rate of deposition by using an oxygen-argon gas mixture.

During the past three years, the process has been shown to have several advantages for the welding of carbon steel. Some of the advantages claimed for it are as follows: high welding speed, higher arc-time factor, minimum of distortion, freedom from slag and spatter, and the penetration characteristic that is typical of inert-gas metal-arc welding. These have enabled the process to be used in production with significant savings compared to former methods of welding. Some of those applications will be discussed here and the particular advantages that led to adoption of the process will be emphasized.

It is important to realize that shielded-inert-gas metal-arc welding, frequently called sigma welding, may not be suited to all applications. At the present time with known techniques and commercially available materials, it is recommended that applications of the process should be selected carefully. In time, it is very possible that its use will be expanded in much the same manner as the Heliarc welding process has developed during the past ten years. Such advancement will come as a result of experience, improvement in materials and techniques, and lower material costs.

Process limitations have been covered in a paper* presented during the Thirty-Fourth National Fall Meeting, AWS. Interesting data were presented which showed that both carbon content and deoxidation level in steel exert an influence on type and extent of porosity in welds. There are wide variations in carbon and silicon contents of a given grade of steel. Of course, these variations in the chemistry give wide differences in weldability. This makes it impractical to estimate weldability in the general terms of rimmed, semi-killed or killed steel designations. To assure satisfactory weld deposits, a knowledge of base metal chemistry is necessary. Where relationship of carbon to silicon content is roughly two-thirds, or lower, the use of killed electrode wire is reasonably certain to produce good welds. Deoxidized electrode wire has to be used when the relationship is greater and the degree of deoxidation depends on this relationship.

A carbon-oxygen reaction is believed to be the prime cause for porosity in carbon steel weld metal. Of equal importance is the occluded oxygen in the plate to be welded as well as any rust and mill scale present. Therefore, surface condition of the joint is most important when fillet welds are involved. Invariably, good results are obtained on pickled or sandblasted plate.

Once we thoroughly understand the effect that material specifications and joint design exert upon weld quality, then practical applications of the process can be pursued. The high welding speeds and high arc-time factors are most valuable on repetitive, high-production types of jobs. Mechanization of the process shows the best ratio of consumable material costs to productive labor and overhead costs. However, where operator arc-time can be maintained at 60% of total time, costs of manual sigma welding will be found lower than manual coated-electrode arc welding.

An outstanding application that is fully mechanized is found in the manufacture of domestic refrigerators. The compressor housing assembly shown in Fig. 1 is positioned with its axis 32 deg from the vertical and rotated beneath a stationary welding head in a fully automatic fixture. The operator's only function is to place the assembly in the fixture, hold it until the machine clamps it in place, and remove it at the end of the cycle. Pushing a single button starts the carefully timed operation that involves the following steps:

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Fig. 1 Compressor housing for domestic refrigerator

(1) start rotation, (2) start flow of inert gas and torch cooling water, (3) arc on, (4) arc off, (5) stop rotation, (6) stop gas and water flow and (7) release clamp.

The housing material is cold-rolled low-carbon steel, with a $^1/_8$-in. outside shell thickness and $^3/_{32}$-in. inside material thickness. The lap-fillet joint is welded at a speed of 70 ipm. Floor to floor total time is approximately one minute per assembly. An argon-oxygen mixture containing 5% oxygen is used at the rate of 45 cfh. Since cold-rolled, low-carbon steel is involved, a commercial killed electrode wire is satisfactory. The electrode is $^3/_{32}$ in. in diam and is used with 390 amp DCRP.

The method formerly employed was a fully automatic open-arc welding process using a wash-coated wire. About 10% of the compressors produced were imperfect and revealed leaks when tested under pressure. Perhaps one might wonder why fully automatic submerged melt welding was not used. The process was tried and rejected when it was found that small particles of welding composition sifted into the interior of the assembly and caused about 10% of them to fail. Naturally, repair at this stage was very costly. An enlarged view of the joint fit-up pictured in Fig. 2 shows how this occurred. Shielded-inert-gas metal arc has been adopted now in preference to submerged melt welding on this type of joint and rejects have been reduced to under 3%. Actually, on one test run, only one leak occurred in 390 welds.

Manual welding with inert-gas metal-arc process is used in the fabrication of compressor housings of air-cooling units for home and plant. Four circumferential welds are needed to make this $^1/_4$-in. thick carbon steel housing. The first of these welds is done by submerged melt welding since the interior of the housing can be cleaned following this weld. However, the three subsequent welds cannot be cleaned and so they are made by sigma welding. For the last joint, the newly developed whipping technique is also used. Since the

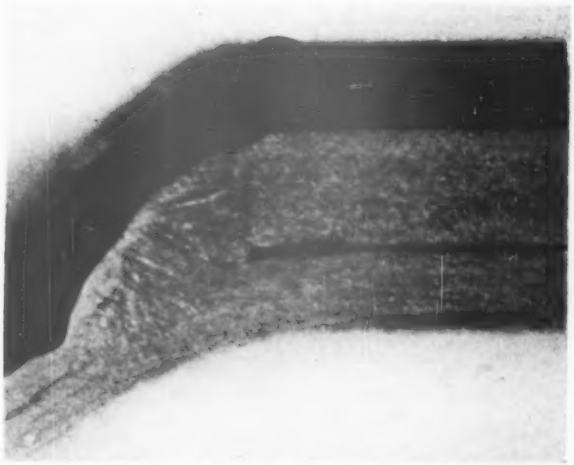

Fig. 2 Enlarged section of lap joint in refrigerator compressor

assembly at this point contains oil, capillary action sometimes causes presence of oil in the joint during welding. The preheat produced ahead of the molten puddle by this whipping action burns away the oil before the molten puddle can reach it. A killed electrode wire $^3/_{32}$-in. in diam at 400 amp DCRP is used for all joints that are welded by inert-gas metal arc. Again a mixture of argon plus 5% oxygen is used at a rate of 50 cfh. Hand welding speeds are from 12 to 15 ipm with an operating arc time of 75% or better.

Another item suited to high production by shielded-inert-gas metal-arc welding was used in the building of Army M-135 trucks by a large automotive firm. Twenty welding booths were used to fabricate rear spring suspension brackets and secondary spring and bumper brackets. Figure 3 shows five of eight booths which were used to fabricate the spring suspension bracket. A conveyor carried the parts to and from the welding booths. Twelve parts were involved in the

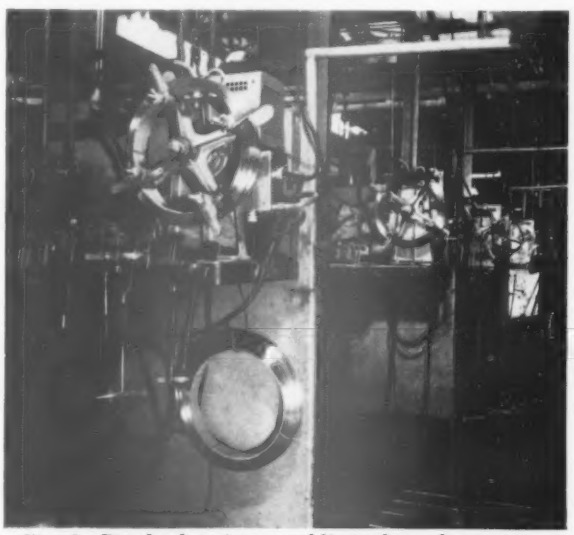

Fig. 3 Booths for sigma welding of truck parts are served by conveyor line

Fig. 4 Spring suspension bracket is held in simple positioning jig for sigma welding

fabrication of the $^3/_8$-in. steel suspension bracket. Open arc welding was used on some of the smaller parts. The final assembly was inert-gas metal-arc welded. Figure 4 shows a closeup of actual welding, while the completed assembly and the parts which make up the bracket are shown in Fig. 5. A positioner held these parts so that all welding was done in the downhand position. Twelve booths were used in the fabrication of secondary spring bumper and body brackets. The bumper plate is $^3/_8$ in. thick and the saddle $^1/_4$ in. thick. Figure 6 shows the parts prior to welding (left), after tack welding (center) and after shielded-inert-gas metal-arc welding (right). Figure 7 shows a closeup of actual welding. The spring suspension bracket fillet weld was made using $^1/_{16}$-in. diam wire, with 425 amp DCSP and a flow of 35 cfh with a mixture of 5% oxygen, 95% argon. A total of 108 in. of welding was required per assembly. The secondary spring bumper and body bracket fillet weld was made using 360 amp DCSP at a welding speed of 35 ipm. The standard mixture of 5% oxygen, 95% argon was used at a rate of 30 cfh. Total length of welding per assembly was 43 in. The contract for this work is now completed and plans are being executed to convert to civilian truck fabrication.

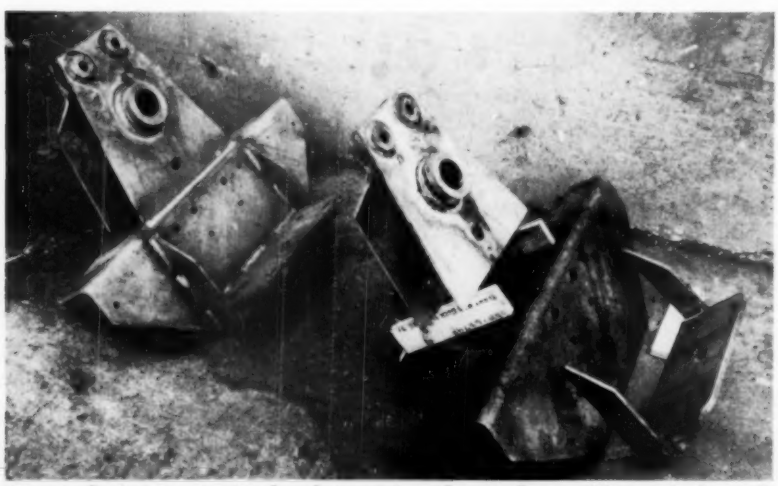

Fig. 5 Spring suspension bracket consists of two subassemblies and a formed plate as shown on the right. At the left, is the completed weldment

A large manufacturer produces oil-fired boilers for domestic heating systems that are fabricated from $^3/_{16}$- and $^1/_4$-in. SAE 1020 carbon steel. These boiler assemblies consist of a center tube section and two formed shapes as shown in Fig. 8. Three manual shielded-inert-gas metal-arc welds are required for each assembly: two fillet welds connecting the center tube section to the two formed shapes and another fillet weld to connect the two formed sections. Top and bottom views of the completed assembly are shown in Fig. 9. To provide ease of welding and to control undercutting, the assemblies are positioned at about 45 deg and $^1/_{16}$-in. wire is

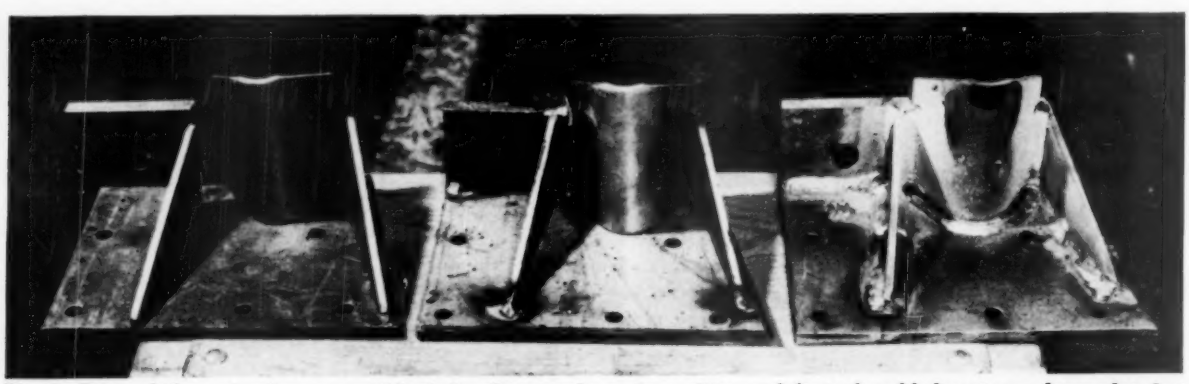

Fig. 6 Parts of the spring bumper and body bracket are shown in position at left, tack welded center and completed at right

Fig. 7 Jig with universal swivel holds spring bumper and body bracket for welding

Fig. 9 Two fillet welds and a girth weld make the boiler weldment. Top and bottom are shown

Table 1—Welding Conditions for Fabrication of Condensers

	80 ipm	*40 ipm*
Arc time per weld, sec	14.15	28.3
Welding current, amp DCRP	600	500
Arc length	Short	Short
Arc voltage, v	24	24
Shielding gas flow (5% oxygen, 95% argon), cfh	40	40
Wire, in. diam	3/32	3/32
Deposition rate, lb/hr	17.2	14.2
Rod consumed per weld, lb	0.066	0.108
Gas consumed per weld, cu ft	0.15	0.30

used with a current of 250 to 300 amp DCRP. Shielding gas is a mixture of 5% oxygen, 9[illegible]% argon used at the rate of 50 cfh. Average speed of welding is 15 ipm.

A large company manufacturing air conditioning equipment has fully mechanized the shielded-inert-gas metal-arc welding process which it uses to join 3/8-in. thick tube-sheet ends to 6-in. OD by 1/8-in. thick condenser tubes. Speeds between 40 and 80 ipm are ob-

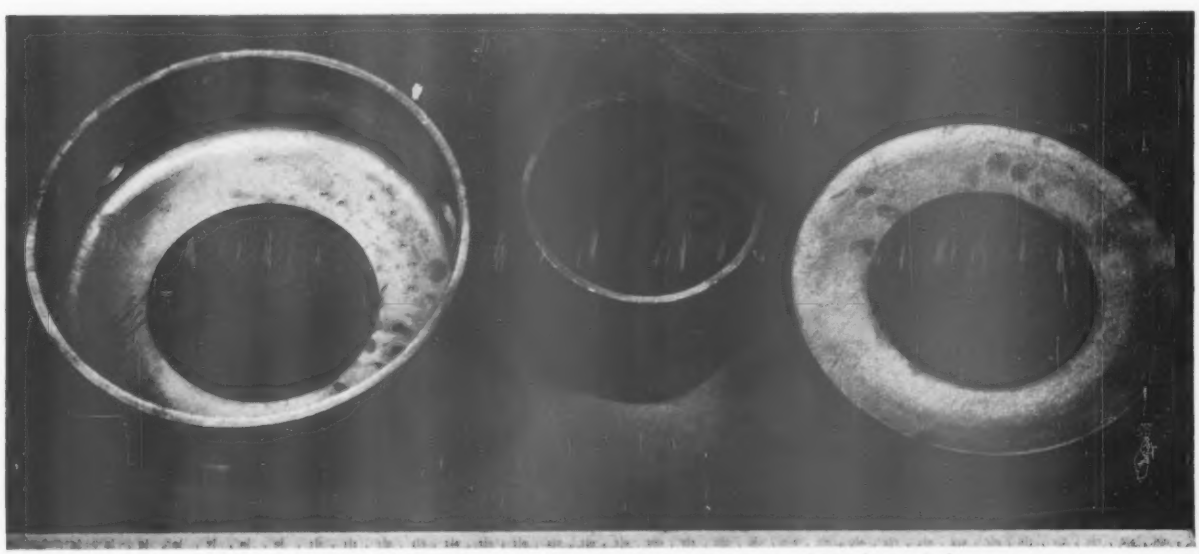

Fig. 8 Oil-fired boilers are assembled from a tube section and two formed shapes

Fig. 10 Drawing shows positioning of assembly for welding large tubes to tube sheet

tained. Welding conditions for these two speeds are shown in Table 1. Operation at the higher end of the range requires some preheat in the heavy SAE 1027 steel sheet end to prevent cross checking in the weld. All welds must pass 180 lb per sq in. leak test. Figure 10 shows how the assembly is positioned for welding.

Consideration was given to the use of submerged arc welding when mechanization of this particular unit was first suggested. Welding had to be done with tubes in place inside the condenser shell. With this process, a backing behind the joint would be necessary. Thus the outside tubes would be separated

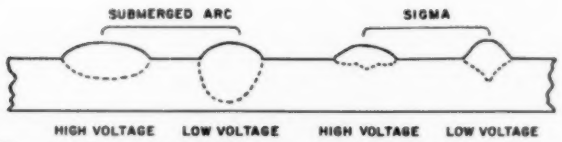

Fig. 11 Penetration characteristics of shielded-inert-gas and submerged-arc welding on flat plate

from contact with the inside diameter of the shell by an amount equal to the backing member thickness. That useful distance could not be sacrificed and so submerged melt welding was not used. With shielded-inert-gas metal-arc welding, the penetration characteristic permits welding at high amperage, and no backing media is required.

The effect of voltage upon the penetration characteristic of submerged arc and sigma welds is shown in Fig. 11. Where submerged arc welding is used for joining plate, the edges should be prepared with a double-vee but with suitable nose thickness to hold the first pass. There is danger of drop-through when the root opening exceeds 0.020-in. If a manual arc weld with coated electrode is used to fill large root openings, then it is necessary to chip out all fused slag prior to submerged arc welding. Rather than lose the fixture time (platen time in shipyards) that would be involved, shielded-inert-gas metal-arc welding is now being used under these conditions. This method is quick and it can be followed within a few feet by automatic submerged arc welding. Thus, time is saved and costs are kept low. Fully automatic welding is used to its highest efficiency, and total time per weldment on fixture or platen is decreased.

Pressure vessel work requires back-chipping or grinding prior to making the first backing pass when using submerged arc or metal arc welding. It has been found that back chipping is unnecessary when the root pass is made by shielded-inert-gas metal-arc welding. A manufacturer of pressure vessels and annealing boxes found that code radiographic requirements for butt welds in pressure-vessel quality plate were met by using preparations and welding conditions illustrated in Fig. 12.

In Canadian shipyards, Lloyd's Register of Shipping has approved downhand welding of ship plate up to $^1/_2$-in. thickness, using standard killed welding wire

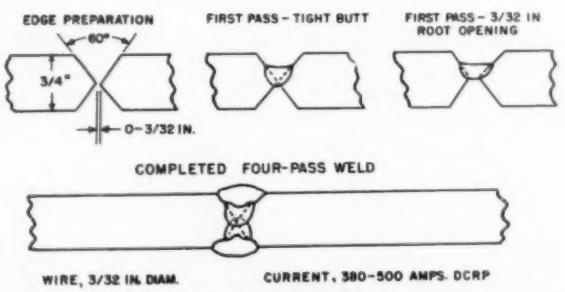

Fig. 12 Plate preparation and bead contour in pressure vessel fabrication

Fig. 13 Frames for large size casters are made by shielded-inert-gas metal-arc welding

and oxy-argon mixture. All specifications for physical properties and radiographic requirements were met for butt welds and fillet welds in ship-quality steel. The most practical application is fillet welding on intercostal members and some butt welding on corrugated tanker bulkheads. In those cases, submerged arc welding cannot be used easily on the contours and shapes of articles being welded. Similar requirements were met in an American Shipyard. On specific application, sigma welding costs have been from 20 to 30% lower than those using flux-coated electrode welding, despite the higher material costs for filler wire and shielding gas. The lower cost of sigma welding is not due to increased welding speeds or different welding currents alone, since the comparison of the two processes is extended by making sample weldments using the same welding currents and speeds. Also, distortion is reduced to one half of that produced by metal arc welding on welds of similar sizes. Sometimes it is even less. This is an important factor where straightening costs are included in the final cost of a completed weldment.

Weld spatter is eliminated by using direct current, reversed polarity. This found good application in assembly of trusses that are fabricated by fillet welding $1^1/_2$- x $^3/_{16}$-in. straps to 2- x 2- x $^1/_4$-in. angle stock. Welding without flux is particularly advantageous for fabricating towers and poles that are subsequently galvanized. This eliminates any attack of the galvanizing that otherwise could be caused by any trace of residual flux. Both the appearance and durability of finish painting is enhanced by absence of spatter. Particularly noticeable is the saving of labor in those cases where spatter would have to be removed prior to painting.

Casters for hand trucks are now fabricated by shielded-inert-gas metal-arc welding. Formerly produced by metal arc welding they had to be cleaned of spatter by tumbling. With no spatter present, casters are now ready for painting after welding. A simple jig holds the parts in position for welding as shown in Fig. 13 and four fillet welds are made per caster. Figure 14 shows the three parts that are required for the caster frame. In back are two finished frames and at the left, a finished caster. The shielded-inert-gas welding process doubles production of the casters over metal arc welding and eliminates cleaning, finishing and fumes.

Fig. 14 Three pieces make the frame for the welded caster. Four fillet welds are required

Carbon steel containers for submarine depth charges are being produced by shielded-inert gas welding. These so-called "squid crates" are in the form of a container in which tee sections, 1- x $^1/_8$ in. are joined to steel stampings that form the end parts. The completed crates are about 6 in. long and 18 in. in diameter. Each crate has 168 fillet welds, $^3/_4$ in. long, made on 2-in. centers. The welding is done manually in a special holding fixture using $^1/_{16}$-in. standard killed welding wire at 225 amp DCRP with a flow of 35 cfh oxy-argon mixture. Close to 30,000 units have been produced in this manner. Production was increased from 12 to 40 units per day. The crates are galvanized after welding, and the clean welding job provides an excellent surface for galvanizing without finishing in any way.

An auto specialty manufacturer uses the process to repair defects in tank tread castings as shown in Fig.

Fig. 15 Shielded-inert-gas metal-arc welding is used to repair tank tread castings

Fig. 16 Repairs on castings are smooth and clean. Usually they require no further finishing

Table 2—Comparative Costs in the Fabrication of Welded Machine Supports

Shielded-inert-gas metal arc		*Flux-coated metal arc*	
Labor 3 hr @ $1.80	$ 5.40	Labor 7 hr @ $1.80	$12.60
Overhead, 300% labor	16.20	Overhead, 300% labor	37.80
Shielding gas 100 cu ft oxy-argon	11.00	Electrode, 12.5 lb rod @ 0.12/lb	1.50
Electrode 7.5 lb std. killed wire @ 0.35/lb	2.65		
TOTAL	$35.25	TOTAL	$51.90

Table 3—Comparative Costs of Competitive Manual and Mechanized Welding Methods

Operating factors vary from shop to shop. Those shown here are assumed for the problem and will be found to lie in the range of such values generally encountered in industry.

Welding method	Operating factor, %
Sigma—manual	60
Sigma—machine	50
Submerged—machine	35
Coated electrode—manual	40%

Overhead cost is assumed to be *200%* of labor cost.

Welding process and cost data for $^1/_4$-in. steel-butt joint

	Hand welding		*Machine welding*	
	Coated-electrode metal arc	*Sigma*	*Submerged melt*	*Sigma*
Welding speed, ipm	8	10	24	15
Welding current, amp	280	340	800	420
Arc time, hr/100 ft of weld	2.5	2	0.84	1.33
Gas consumption, cu ft/100 ft	..	100	..	67
Welding rod, lb/100 ft	30	20	17	20
Welding composition, lb/100 ft	..	..	25	..
Labor cost/100 ft @$1.75/hr	$10.90	$ 5.83	$ 4.20	$ 4.66
Gas cost/100 ft	..	11.00	..	7.37
Rod cost/100 ft	4.50	6.40	2.21	6.40
Power cost/100 ft	0.50	0.50	0.50	0.50
Welding composition cost/100 ft	..	..	2.62	..
SUBTOTAL	$15.90	$23.73	$ 9.53	$18.93
Overhead	21.80	11.66	8.40	9.32
TOTAL per 100 ft of weld	$37.70	$35.39	$17.93	$28.25

NOTE: At 60% operating factor, sigma welding is less costly than coated electrode manual welding at 40% operating factor.

15. Sigma welding is faster than flux-coated metal-arc welding and the sigma weld deposits require no cleaning or peening. The deposits shown in Fig. 16 are smooth and clean, and usually require no finishing at all.

Core boxes are reconditioned by a steel castings company. Fillet welding repairs are made at 18 ipm with sigma welding compared with 6 ipm using coated electrodes.

Laminated packs such as those for field stators in electric motors are welded using both inert-gas tungsten arc and inert-gas metal arc. The latter is used when the steel contains over 2.5% of silicon, or when openings in the stack rule out tungsten arc. Welding speed as high as 75 ipm is obtained without cracking. The speed of assembly in fully mechanized welding and handling provides a great increase in production when compared to rivet type assembly. It also eliminates rivet holes and allows greater efficiency in the same OD field core by design change and less eddy-current loss.

Making welded supports for testing machines provided an interesting comparison of production costs for coated electrode welding vs. inert-gas metal-arc welding. A total of 30 ft of $^3/_8$-in. fillet welding was required to join 1-,$^1/_2$- and $^1/_4$-in. plate sections. Welding times after fitting and tacking are indicated in Table 2. No preparation, grinding, etc., was used. This indicates the importance of considering overhead charged against labor when making cost comparisons.

Perhaps Table 3 illustrates this point more clearly. It gives a cost comparison of doing a typical job by four methods. The material used is $^1/_4$-in. steel plate and the job, a flat butt joint. You will note that the cost of manual arc is considerably less than manual sigma if no consideration is given to overhead. However, when overhead is included, total cost of sigma welding is less than flux-coated metal-arc welding. In the mechanized applications where the design and conditions are favorable, submerged arc welding can be considerably less costly than sigma. Tables such as this show us that all costs must be carefully analyzed so that the most favorable welding method can be selected.

High-Temperature Alloy Fusion Brazing for Titanium and Titanium Alloys

A titanium-nickel-copper-cobalt alloy has been developed which gives good results in furnace brazing titanium and titanium alloys

by Roger A. Long and Robert R. Ruppender

THE brazing of titanium, commercially pure or alloyed, has been based to date on the use of silver or silver base alloys. Considerable data has been published in the literature by many research foundations and industrial laboratories. The indications are that the use of pure silver with a proper flux and induction heat gives the best and most consistent results.

All of the literature reports stress minimum time cycle requirements, because of the formation of brittle intermetallic layers, by diffusion. Diffusion of elements into one another is a well-known metallurgical phenomenon and is dependent upon time and temperature. The formation of bands or layers of intermediate phases is dependent upon the number of phases in the system. These bands form as diffusion progresses. The brittle layers create planes of weakness which in turn cause low-strength and low-ductility joints.

The use of furnace brazing for titanium thus becomes almost impossible with silver base materials as the time cycle required would be shorter than commercially practical.

It was, therefore, necessary to develop a new series of alloys if furnace heating was to be utilized. This meant the development of an alloy which had a flow temperature within the metallurgical limitations of the titanium base metal, and did not alloy by strictly a diffusion process.

It, therefore, became apparent that a "fusion" brazing alloy instead of "diffusion" alloy was needed. By utilizing the word "fusion," we have redefined brazing to some degree, and in the light of new practices in the last two to five years, it would seem that a new definition for brazing would be apropos. We have, therefore, redefined brazing as follows:

"A method of joining two or more metallic surfaces at a temperature below either of their true melting points, by the use of a third metal or alloy wherein alloying takes place at the interfaces by fusion and/or diffusion. Such alloying promoting the formation of a strong bond."

Fusion alloying would thus tend to minimize the formation of intermetallic layers and the resulting planes of weakness at the interfaces. Any intermetallic phases formed would be distributed throughout the joint area and may actually act as joint strengtheners.

Furnace brazing by utilizing protective gas atmospheres instead of fluxes is most desirable. Because of the affinity of titanium for oxygen, nitrogen and hydrogen, the use of inert gas, such as argon or helium was dictated. It was found that tank argon gas properly cleansed was satisfactory and this gas has been used in all of our experimental work. An ordinary resistance tube wound muffle furnace was utilized for all work.

A brazing filler alloy must meet the following requirements:

1. It must melt and flow freely at a temperature which will not adversely affect metals being joined.
2. It must alloy with the parent metal with little or no formation of brittle phases, particularly in layers.
3. The alloy phases formed at the interface must have strength and ductility equivalent to the minimum required for any particular application.
4. Washing or erosion between the brazing filler metal and the parent metal must be controllable within the limits required for the brazing operation.
5. The temperatures required for braze filler metal flow and to which the parent parts are subjected must not be so high as to result in detrimental structural changes with resulting low mechanical properties.
6. The alloying elements of the braze filler metal must have low volatilization characteristics at brazing temperatures.
7. It is desirable that it alloy or combine with the parent metal to form an alloy with a higher melting temperature.

In addition to the above basic requirements, it was

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Presented at the AWS National Spring Meeting held in Buffalo, N.Y., week of May 1–5, 1954.

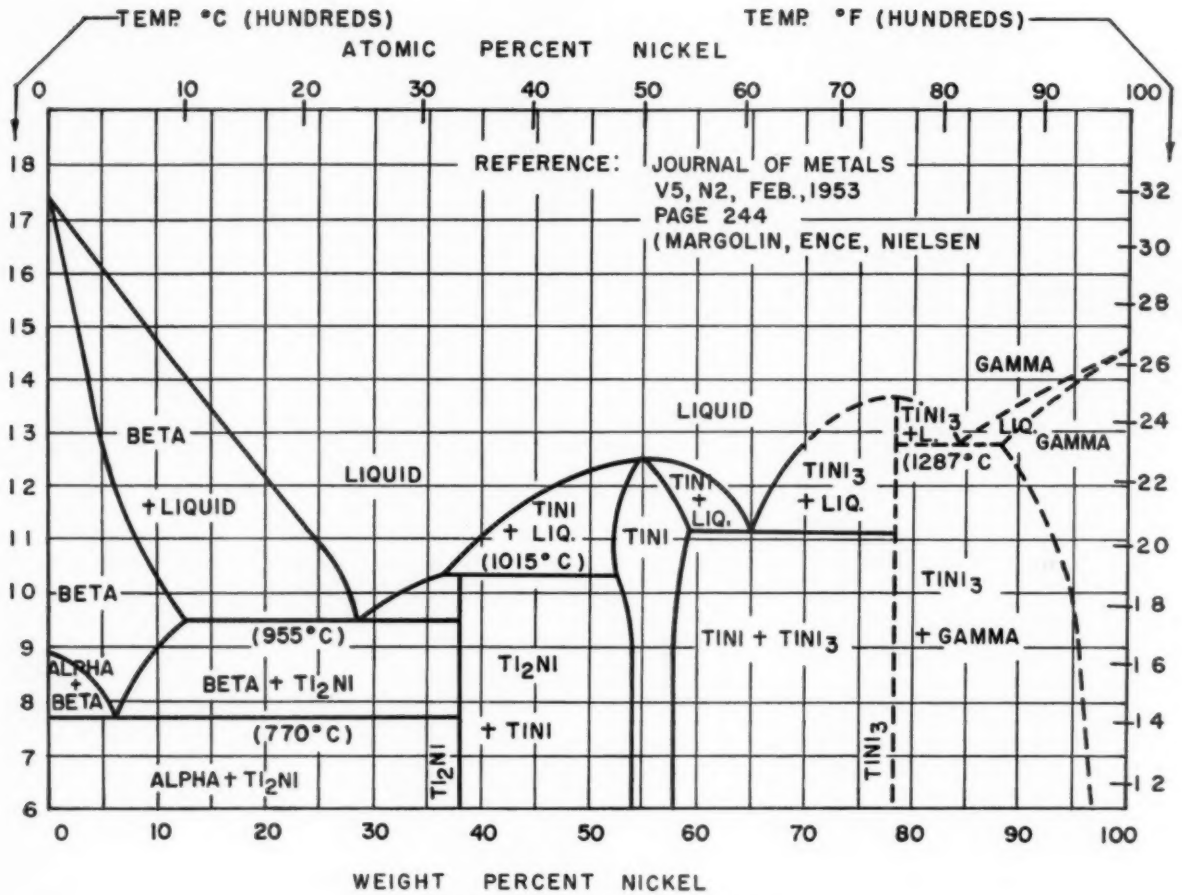

Fig. 1 Titanium-nickel phase diagram

desirable to select an alloy system which would be compatible with titanium, and if possible, one that would be self-fluxing to some degree. By self-fluxing, we mean an alloy or metal which will dissolve the oxides and nitrides of titanium to some small extent, but sufficient to allow a commercial atmosphere to be used. Examination of the various phase diagrams indicated that the choice of a system was limited either in that the melting points were too high or that the elements one would have to select would have little strength in combination.

A preliminary analysis indicated that the nickel-titanium system looked the most encouraging. This system is shown in Fig. 1. It will be noted that there are two usable eutectics, one at about 66% nickel and the other at about 28% nickel. The higher nickel eutectic melts at about 2030° F, while the lower melts at about 1750° F, according to the literature. If one adds titanium to either, there is a rapid increase in solidus temperature, indicating that brazing may be possible without appreciable "washing" effect.

It can be seen from Fig. 1 that as we move to the left, or in other words, as we braze and alloy, titanium is dissolved and new phases are formed. The number of new phases formed within the joint area are those phases present to the left of each eutectic. The low nickel eutectic results in a minimum number of resultant phases and from this aspect looked very interesting. It is desirable in most cases, to have solid solution phases formed whenever posible. The higher nickel eutectic has a greater probability of forming undesirable brittle phases, but has advantages in that the properties are not affected by small gaseous elements such as would be the case with the high titanium eutectic.

Experimental work was initiated on both titanium-nickel eutectics. These approximate compositions were available in fine powder form from Metals Hydrides, Inc. Initial flow temperature data indicated that temperatures in excess of 1950° F were necessary for flow and as such were primarily responsible for embrittlement of the base parent metal. The authors feel that oxygen from furnace leakage and nitrogen impurity in the argon are more active at the high temperatures. These gases, when in contact with titanium, would be absorbed faster the higher the temperature. Therefore, at the higher temperature, their effect would be more pronounced. The reason for the high melting temperature of the high titanium eutectic is not known but the material we utilized was at least 200° F higher than the 1750° F shown on the phase diagram, Fig. 1.

Samples of commercially pure titanium were run in the argon atmosphere furnace for various times and temperatures. From these tests, a maximum per-

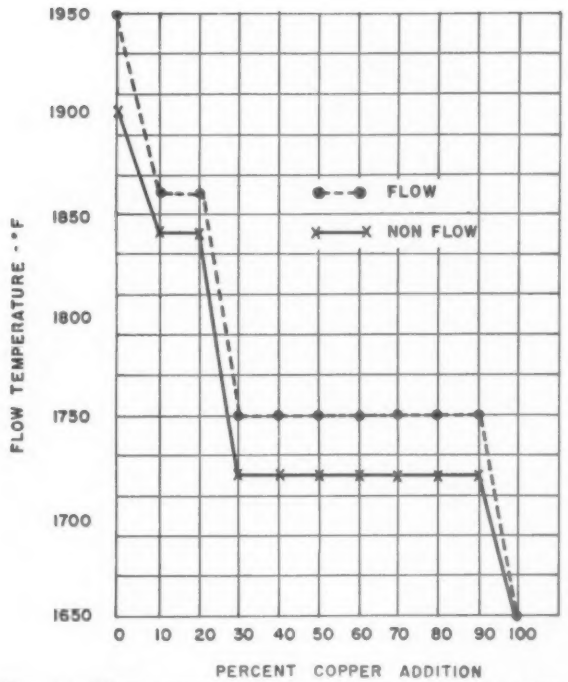

Fig. 2A Flow temperature of "A" series brazing alloys with copper additions when in contact with titanium metal

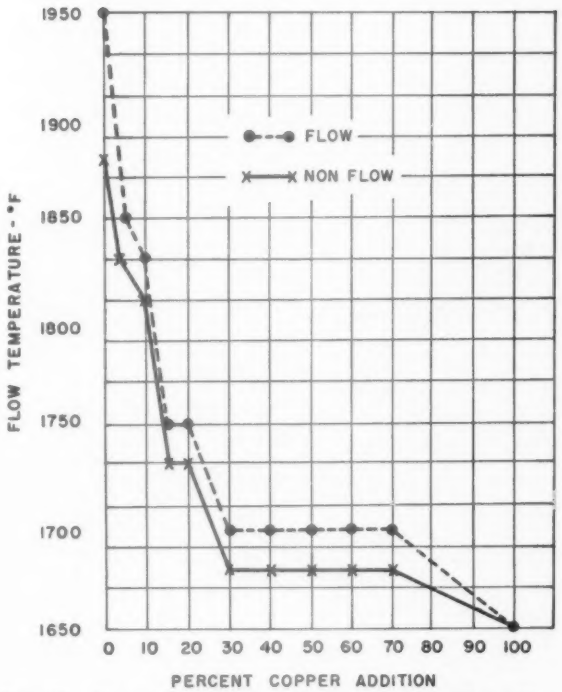

Fig. 2B Flow temperature of "B" series brazing alloys with copper additions when in contact with titanium metal

missible temperature of 1950° F was indicated and time periods up to 30 min were found to be satisfactory. This established the very top limit of our brazing conditions as to commercially pure titanium, and dictated the selection of the brazing alloy.

Wettability of the brazing alloy is dependent upon the base metal, the brazing alloy composition and temperature. Temperature is one of the most effective means of increasing wettability. However, because of having to limit our maximum flow temperature, we could not take advantage of this factor. Brazing filler alloy composition seemed to be the next best approach and a development program was initiated.

Additions of copper were found to be the most expedient in lowering the flow temperature of both titanium-nickel base eutectics. This is shown for the high nickel eutectic (A series) in Fig. 2*A* and for the high titanium eutectic (B series) in Fig. 2*B*. However, it was felt that these high percentages of copper would be detrimental to the mechanical properties of the brazing joint because of its known embrittlement characteristics with titanium. Other tests indicated that copper contents ranging up to 15% would be permissible, and would effect a substantial lowering of the flow temperature. Small percentages of cobalt, chromium and manganese, separately or in combination were not found to have a detrimental effect on flow temperature of either base eutectic containing up to 15% copper. In fact, additions up to 4% supplemented the copper and allowed us to use slightly less copper in order to obtain equivalent flow temperatures. This was particularly true with the high titanium eutectic (B series).

Element balancing must also be considered from its possible effects on the braze joint properties. This is particularly important when we are dealing with a "fusion" brazing alloy where there is a fairly wide fusion zone across which the hardness can vary considerably. It was felt that if the hardness of the joint from parent metal to parent metal could be kept uniform or within reasonable uniformity, a better joint could be realized. The various brazing alloys have a hardness as-cast between Rc 25 and Rc 46. The interface hardness after complete alloying with various alloy combinations varies between Rc 25 and Rc 36. Commercially pure (Rc 70) has a hardness between Rc 18–23. Therefore, by proper control of the alloying elements and joint clearance, the hardness gradient can be minimized by proper element balancing.

We have established approximately fifteen different alloy combinations that seem to show promise. We have at this date only partially evaluated two of these alloys. The series A alloy, experimentally designated A7a, has the composition shown in Table 1, and is nickel base. The B series alloy is titanium base, and is being used only as an exploratory alloy for this series.

Table 1—Typical Brazing Alloy Compositions, "A" Series Alloy and "B" Series Alloy

Alloy designation	*Composition, %*			
	Ti	*Ni*	*Cu*	*Co*
A7a	28	52	15	5
B4	64.35	25.65	10	0

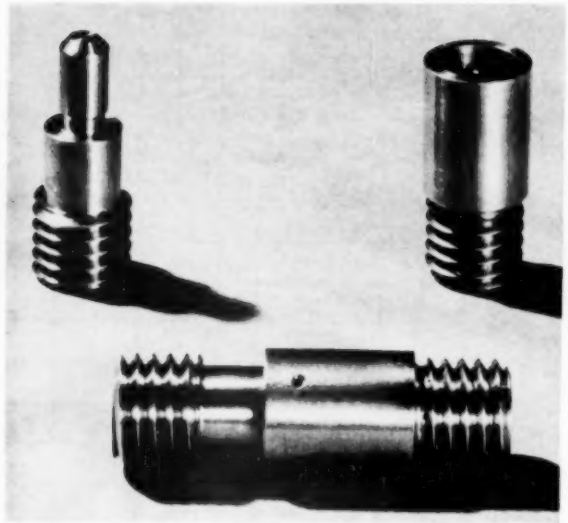

Fig. 3 Tensile shear specimens design for concentricity and axial loading

The other alloy combinations have only been evaluated as to wettability and effects of time and temperature on the flow characteristics.

Preliminary compressive shear data on A7a brazed Rc 70 material has shown shear strengths averaging 59,300 psi with the shear area calculated on a diameter of 0.532 in. and a depth of 0.144 in. Failure occurred in the braze joint. Tensile shear data determined on specimens machined as shown in Fig. 3 averaged approximately 43,000 psi with the shear area calculated on a diameter of 0.395 in. and a shear length of 0.060–0.081 in. All failures occurred in the parent metal outside the brazed joint. The highest value was 47,990 psi and the lowest 37,040 psi.

Compression shear data on the B4 alloy averaged approximately 37,500 psi and failure occurred in the braze joint. The highest value was 39,570 psi and the lowest 35,550 psi.

In order to determine whether the time and temperature influenced the base metal RC 70, a blank tensile specimen was run which was the same size as the shear test specimen. It was furnaced along with the shear specimens. The resulting tensile strength was 110,000 psi which checked closely with ultimate tensile strength of the as-received Rc 70 which was 105,500 psi. The elongation could not be correlated with the as-received Rc 70 data because of the difference in gage length. However, there was appreciable necking down at the fracture indicating that the ductility had not been damaged appreciably.

The metallurgist utilizes metallography as a research tool. Figure 4 shows the structure of the joint

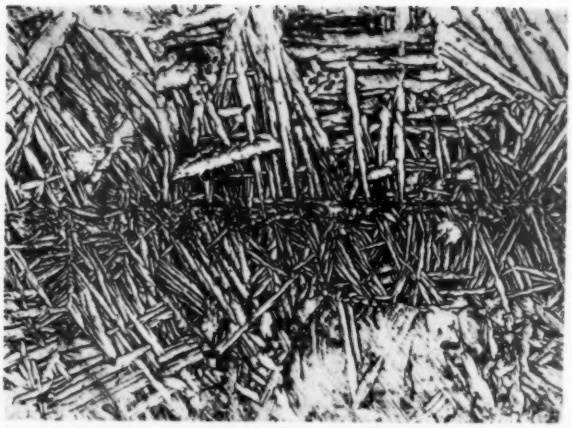

Fig. 4 Braze joint area A7a brazing alloy magnification of 100 times, open magnification 225 times. Etchant hydrofluoric, nitric and water. (Reduced two-thirds in reproduction)

area brazed with A7a alloy. This photomicrograph is typical of the structures obtained with this fusion brazing alloy. The etchant used was nitric and hydrofluoric acids in the ratio of 5:1 diluted with 95 cu cm of water. The fusion area is approximately 0.015 in. wide. The original gap clearance was 0.002 in. There has been no evidence of failure in this zone in any of the tensile shear specimens. The restriction of this zone to a narrow width is necessary and particularly when working with sheet metal parts which are thin and where this zone could amount for a considerable fraction of the gage thickness.

The following conclusions can be drawn from this preliminary data:

1. A "fusion" type alloy has been developed utilizing primarily the eutectics of the titanium-nickel system, such alloy being applicable to protective atmosphere furnace brazing techniques.
2. Compressive and tensile shear strengths in excess of 40,000 psi have been obtained in screening tests.
3. Flow temperatures ranging from 1700 to 1900° F have been proved possible by proper alloying and such temperatures are usable without damage to commercially pure titanium.

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EDITORIAL PREVIEWS

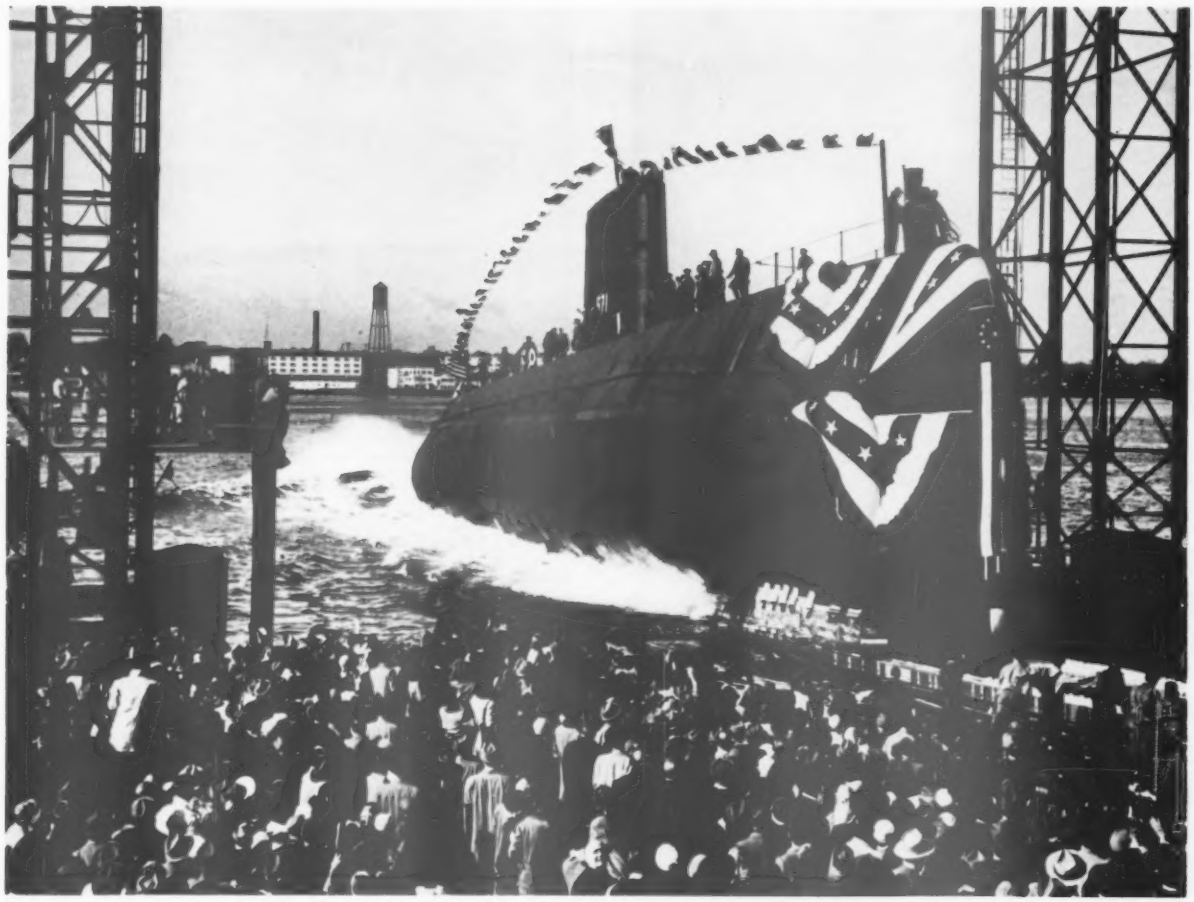

Fig. 1 The world's first atom-powered submarine hits the water

The "Nautilus"

The power of atomic energy makes the "Nautilus" the world's most potent naval weapon*

THE *Nautilus* that carried Prof. Pierre Arronax and his friends on their world cruise in Jules Verne's *20,000 Leagues Under the Sea* was 232 ft long . . . 26 ft in beam . . . displaced 1500 tons . . . and could attain a speed of 50 knots submerged. She was propelled by electricity generated by sodium derived from sea water, and her range was almost unlimited.

Facts came within grasping distance of fiction's coattails early this year with the launching of the Navy's new atom-powered submarine *Nautilus* at the Groton, Conn., yard of General Dynamics Corp.'s Electric Boat Division. The *Nautilus* is the first of two atom subs to be built at Groton. The *Sea Wolf*, now under construction, will be completed in the near future.

The *Nautilus*' submerged speed is "in excess of 20

* Courtesy, *Airco in the News*.

Fig. 2 Mrs. Dwight D. Eisenhower smashes the traditional bottle of champagne against the flag-draped prow, while John Jay Hopkins, chairman and president of General Dynamics, stands by

knots." This in itself is remarkable—the fleet-type U.S. subs that sank nearly 6 million tons of enemy shipping during World War II travelled 20 knots *on the surface*, only 9 knots submerged.

She is about 300 ft long. Running on the surface, she will bear a remarkable resemblance to the new post-war fleet-type *Tang* Class—clean lines, without deck guns or other protuberances, and a tall, finlike conning tower. However, she displaces more water than a *Tang*-Class sub, and has a blunter prow. And her propulsive machinery is something new under the sun. The *Nautilus* utilizes the heat of nuclear fission to generate steam in a completely closed cycle that delivers its power through a steam turbine. Thus, the *Nautilus* is essentially steam-driven.

The Uranium-235 that fuels the heat-generating reaction is expended so slowly that there are virtually no limits to the submarine's cruising range, nor to her ability to remain submerged. The *Nautilus* will be able to range the world's oceans—even those beneath the arctic icecap—without coming up for air.

Fig. 4 Electric Boat Division uses Aircomatic welding for work on special alloys such as aluminum and stainless

Choice of Electric Boat Division of General Dynamics to build the *Nautilus* and her sister ship the *Sea Wolf* was a logical one. Electric Boat has been building submarines for 55 years. Since 1899 more than 330 subs have slipped into the Thames River from the Groton yard.

The U.S.S. *Holland*, the Navy's first submarine, was riveted. Today, riveting has given way to welding as the principal fabricating method used by Electric Boat. Three welding methods were used to build the U.S.S. *Nautilus*. These included metal arc welding with

Fig. 3 A hand-cutting torch is used to trim the shell of a storage tank, prior to its being given a new covering

Fig. 5 An Oxygraph with four cutting torches and magnetic tracer cuts heavy alloy steel plate

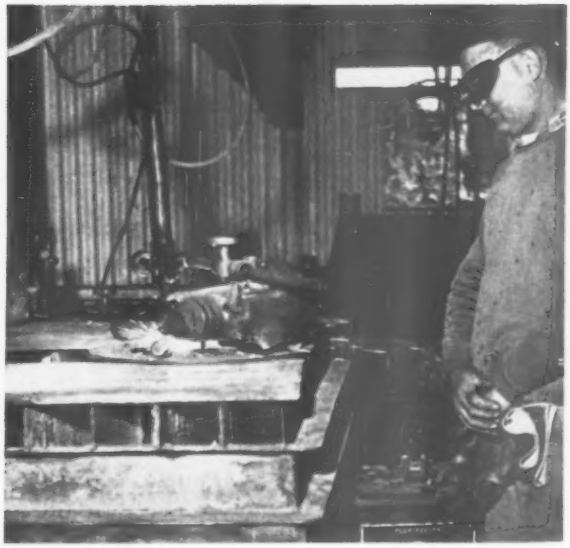

Fig. 6 Stainless steel is cut at Electric Boat with a Radiagraph equipped with an Airco flux injection attachment

Fig. 7 A double bevel is cut in 1½-in. high-tensile low-alloy steel plate with a Radiagraph

coated electrodes, Aircomatic® and oxy-acetylene. No less important was the use of hand and machine flame cutting to shape the heavy steel plates before their assembly into the hull and other structural sections.

Although many types of alloy steel serve aboard the *Nautilus*, these modern fabricating methods had no difficulty dealing with each—and every piece and every weld was tested inch by inch for imperfections. Conventional machine flame cutting and metal arc welding were used for heavy sections of low-alloy steel. Flame cutting with flux injection and the Aircomatic process provided effective methods of cutting and welding stainless steel, a vitally important material in the new sub's atomic power plant.

Sleek and formidable as a barracuda, the *Nautilus* houses her striking power in a rugged, sea- and combat-worthy shell. While we can hope she will never be tested in war, the *Nautilus* exists for our protection, and her very existence will give pause to a would-be aggressor.

Welder Uses New Principle

Unit regulates arc and wire feed automatically—retains visibility and flexibility of manual welding

A NEW semiautomatic welding machine is now being field tested by the Amsco Division of American Brake Shoe Co. It combines the advantages of hand welding and automatic welding, while eliminating most disadvantages encountered with either method.

Amsco's new machine, called the MF Welder, feeds bare wire through a hand-held hopper containing magnetic flux. Flux clings to the wire as it leaves the hopper because of the magnetic field set up by welding current flowing through the wire. Thus the electrode wire, which is fed continuously from a coil, reaches the arc with a flux covering that contributes the advantages of a manual electrode coating. The operator can weld steadily for 15 min if desired; the delay of replacing electrodes and the waste of stub ends are eliminated.

Conventional (ferrous) automatic welding is only feasible when bare wire is used—which requires covering the weld area with a loose, granular flux. This means covering the arc as well, permitting no observation of the work. Therefore, it is essential in automatic welding, that the work or the welding electrode be traversed and jigged mechanically for proper deposition.

While this is satisfactory for routine, repetitive pieces that are flat or round, it is not economical for irregular pieces or short-run work. Furthermore, a lot of un-

Fig. 1 Portable MF (Magnetic Flux) Welder combines advantages of hand welding and automatic welding

used flux must be handled on every job.

The MF is easily connected to any conventional a-c or d-c welding machine and operates over a current range of from 150 to 500 amp. It consists basically of a hook-up cable, a small chassis mounting an electrode wire reel, motor-driven feed rolls, and a flexible 14-ft tube through which the electrode wire feeds to a hand-held hopper. The portable model is provided with wheels for easy movement, and is pear-shaped to help in pulling it around the job. Weighing only 34 lb it can be hung on a wall, where convenient, by means of a simple bracket. A slightly larger cabinet model is also available, caster mounted, and with ample storage space for various fluxes.

The magnetic flux provides arc shielding, deoxidation, arc stabilization, slag coverage of the molten bead, and in some instances, alloying ingredients. The possible range of flux types is wide. Using only one type of electrode wire and varying the flux to suit, it is possible to deposit mild steel, build-up steels, martensitic steels, martensitic irons, austenitic manganese steel and other alloys. Only seconds are required to change from one type of flux to another, and no waste of flux or electrode is involved.

When an arc is struck the welding current closes a relay which connects a drive motor to the source of welding current, and the motor feeds wire to the arc. Motor voltage (and speed) depend on voltage across the arc. If the operator should pull away from his work, increased arc voltage speeds up the motor to feed wire *faster*. If he leans toward his work, decreased arc voltage slows down the motor to feed wire *slower*. This self-compensating feature holds the arc gap steady at any length set by the operator's control rheostat.

A practically constant arc length makes it possible for the operator to concentrate on accurate weld metal placement and is a great help in training inexperienced welders. To evaluate this factor, a novice who had never handled welding equipment was enlisted, and laid satisfactory welds after five minutes of instruction. The steadiness of the arc, as evidenced by recording meters, was comparable to that of an experienced welder with a manual electrode.

A push button allows the operator to feed wire without striking an arc. When set up for welding, several inches of bare wire project from the hopper nozzle. A pinch of flux can be placed where the weld will begin to make starting easier and to compensate for the small length of bare wire sticking out of the nozzle. Instantly after starting, wire feeding into the arc is coated with magnetic flux from the hopper.

The use of magnetic flux and a magnetic gate on the hopper nozzle is a revolutionary advance, opening wide areas for more efficient welding. The gate consists of several tiny permanent magnets in the walls of the nozzle orifice. When the machine is not welding, the flux clings to these magnets in tufts and thereby blocks the annular opening around the electrode wire. When welding starts, however, the magnetic field around the electrode wire is sufficiently strong to drag the flux along with the wire as it leaves the gate. The amount of flux (by weight) is influenced by density and granule size, both of which can be controlled, and also by the outside diameter of the magnetized flux coating. The latter depends on the size of the orifice, and can easily be varied. The control orifice cap is light and inexpensive and a bayonet fastening permits quick changes. A series of caps with graduated sized holes are furnished with each machine and cost so little as to be expendable.

SUMMARY OF FEATURES

1. The speed and convenience of submerged melt

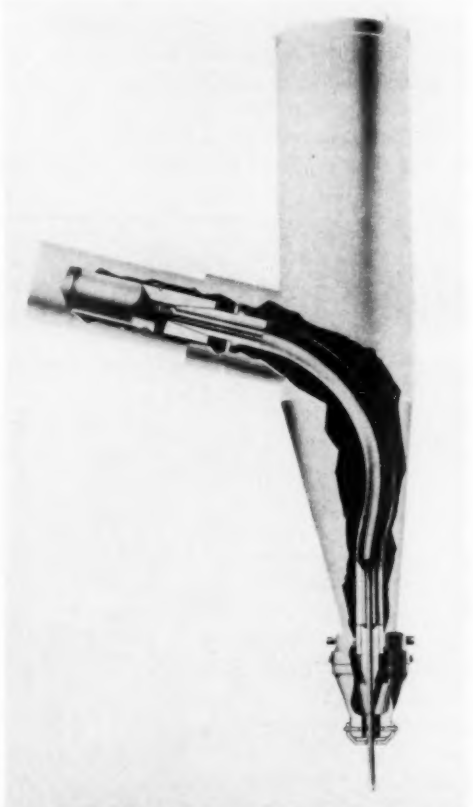

Fig. 2 Cross section of handheld hopper of MF Welder. Magnetic flux clings to permanent magnets in tip of nozzle when not welding. When welding starts, bare wire feeds in from left automatically and the strong welding current attracts a coating of magnetic flux as wire leaves nozzle

technique but with the visibility and flexibility of manual welding.

2. Adaptability to a variety of weld metals, including mild steel, manganese steel, hard-facing alloys, and semiautomatic submerged welding.

3. Quick changeover from one type to the next.

4. Good deposition efficiency versus submerged melt welding.

5. Minimizing of weld craters.

6. Elimination of electrode stub ends.

7. Easy instruction in holding an arc and quick learning for novices.

8. Simple, portable equipment.

Atomic Research Reactors

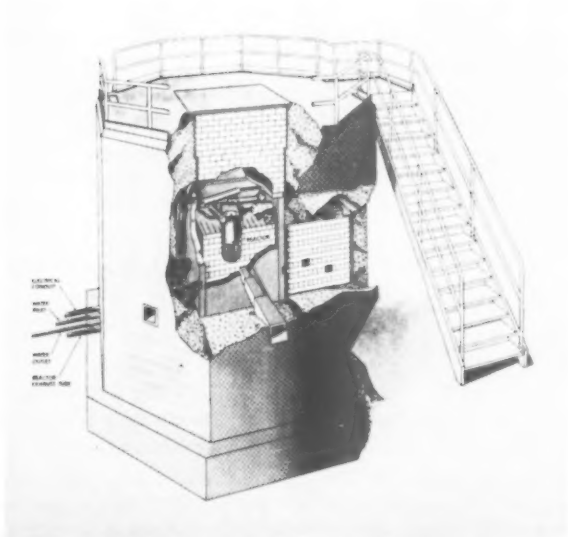

Fig. 1 "Water boiler" research reactor

TWO relatively low-cost atomic research reactors, adapted for use in training badly needed personnel for the nuclear power industry, as well as for conducting basic nuclear research and development, have been placed on the market by the Atomic Energy Division of The Babcock & Wilcox Company according to C. H. Gay, vice-president in charge of the division. B&W's designs are modified versions of the declassified "water boiler" and "swimming pool" reactors developed at Los Alamos, Oak Ridge and other installations of the Atomic Energy Commission, Mr. Gay said, "and will sell for a firm price, depending upon specifications and installation problems, somewhere in the range of $100,000 to $150,000."

"If the present atomic energy law is changed to permit this new industry to progress on a normal free enterprise basis, we estimate that its future requirements for physicists and engineers will probably be at least 6000 more in the next eight years," Mr. Gay pointed out. "However, at the present time, colleges and universities simply do not have the facilities properly to train the men to fill these jobs. This estimate pertains only to those people who will be engaged in the manufacture of atomic energy equipment for the production of electric power.

"In addition to the heavy responsibility for preliminary training of men, our nation's colleges and universities must bear a part of the tremendous burden of research which still lies ahead in the field of atomic energy. They must do so despite the fact that many of our advanced schools are handicapped by the necessity for operating on relatively low budgets. It is with these thoughts in mind that B&W's 'water boiler' and 'swimming pool' reactors are being offered."

John W. Landis, B&W nuclear engineer, said that both the "water boiler" and the "swimming pool" units have been designed for a wide number of uses. They are both useful in neutron experiments, radiation chemistry, biological research, radioisotope production and materials development, in addition to their value in engineering education. The "swimming pool" reactor will probably cost more than the "water boiler"

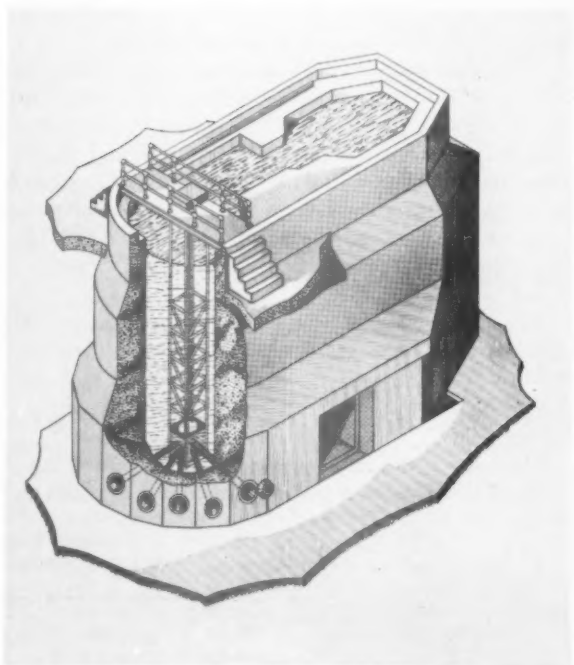

Fig. 2 "Swimming pool" research reactor

reactor, but will also offer certain advantages such as space for bulk shielding and similar experiments, which the "water boiler" reactor does not provide. On the other hand, the "water boiler" has been optimized to obtain the highest possible neutron flux per dollar invested.

"As far as cost is concerned, however," he said, "the most significant advance is that we are now able to offer a firm price on both the manufacture and installation of either reactor. We believe that every large university and college in this country will eventually have a research reactor as standard equipment in their laboratories."

Mr. Landis, who formerly coordinated the technical aspects of the Atomic Energy Commission's Industrial Participation Program, emphasized that governmental approval is required before any institution can go ahead with a program of construction of one of these reactors for either research or radioisotope production.

"WATER BOILER" RESEARCH REACTOR

The Babcock & Wilcox Water Boiler Reactor is a semihomogeneous unit comprising a solution of enriched uranium sulfate in water as fuel and moderator, and a separate cooling system of unique design. Its rated power is from 200 to 400 kw, but it can be operated at any power below this level.

The uranium sulfate solution is contained in the lower half of a cylindrical vessel with spherical bottom. The vessel is 10 in. ID by 3 ft high. The shell is $^1/_{16}$ in. thick and is made of Type 347 stainless steel. Boron control and safety rods extend down into the core (solution) from above. The core is located in the center of an approximately 4-ft cube of graphite reflector which, in turn, is surrounded by a 4-in. carbon steel thermal shield. A 5-ft extension of the graphite reflector on one side serves as a thermal column.

The pressure above the fuel solution is maintained at negative 6 in. of water as a safety feature in order that any leakage will be into the reactor gas system rather than out into the reactor room. Hydrogen and oxygen gases created by fission-induced dissociation of the water in the fuel solution are diluted with helium to keep the concentration well below the danger point and are then recombined in a catalyst bed and returned through a closed loop to the core.

The cooling system comprises U-shaped Type 347 stainless steel cooling tubes placed so that they dip down into all parts of the fuel solution and maintain the fuel temperature at a constant level below the boiling point. The coolant is ordinary water.

The reactor and its components are contained in an octagonal, high-density concrete shield. Aluminum exposure ports extend to the core or reflector from all shield faces, and two ports are located in the thermal column. The air space within the shield is maintained at a slightly negative pressure as a secondary safety feature.

The over-all design of the Water Boiler Reactor is similar to that of the Los Alamos water boiler. Introduction of more cooling capacity, however, permits operation at higher power and production of a higher thermal flux than can be achieved with an ordinary water boiler. The total flux density of the unit is in the order of 10^{13} n/cm^2/sec. The unit has been designed for high specific power and high thermal flux. The total power and therefore the critical mass is nevertheless relatively low compared to other types of reactors.

The Water Boiler Reactor makes available strong collimated beams of both thermal and mixed neutrons for neutron velocity selectors and other experimental use. Large through-hole facilities with fairly high fluxes can be provided for engineering physics and irradiation experiments.

"SWIMMING POOL" RESEARCH REACTOR

The outstanding characteristic of the Babcock & Wilcox Swimming Pool Reactor is its high degree of flexibility.

This reactor is a modified version of the Oak Ridge Bulk Shielding Facility reactor. Its maximum power is about 1000 kw. The core is heterogeneous, with a minimum critical mass of 2.75 kg. The pool is approximately 10 ft wide by 20 ft long by 25 ft deep, and is ordinarily equipped with eight 6-in. and two 8-in. beam ports.

The unit can be operated with a variety of core patterns with or without a partial or full reflector of graphite or BeO. Control is accomplished by rods integrated with the core suspension and actuated by a simplified drive mechanism. Instrumentation has also been simplified.

In addition to its standard beam ports, the Swimming Pool Reactor usually incorporates three pneumatic tubes for fast "rabbit" experiments. Provision is made for inclusion of special ports and experimental tubes in a number of different arrangements.

At full power, the core is cooled by recirculating pool water through a heat exchanger at the rate of 1000 gpm. The pool water is continuously deionized to prevent build-up of radioactivity and, in the event of a power failure, the inlet header will automatically disengage to permit natural convection cooling.

The shield is constructed of high density concrete.

Ryan Range Extenders

by William Brotherton

RYAN is building huge wing tanks for Bowing KC-97 Stratofreighters. These mammoth torpedo-like containers are stretching the range of the big aerial tankers to help them make a mid-air refueling on an average of once every 15 min around the clock.

The gleaming cells are a striking example of engineering and production teamwork. Through close coordination between these divisions, Ryan has developed a tank design which is outstanding in lightweight, high strength, simplicity and gas-tightness. The design objective was to create a tank which would meet all of the high requirements of Boeing and the Air Force and lend itself to sound fabrication methods.

KC-97 wing tanks must carry hundreds of gallons of aromatic fuel at 350 mph. They must withstand every G-load, sloshing impact and vibration caused by flight maneuvers. They are subjected to rigid furnace-hot and ice-cold temperature tests. They must be immune to leakage and damage under these tortuous conditions. Their weight must be kept to a minimum.

Boeing outlined the basic tank requirements. These included capacity, dimensions and contours. Given these and the Air Force requirements, Ryan engineers designed the tank structure, fusing engineering and production know-how at every step.

Several years ago, we developed a unique and highly successful tank design with the creation of the big fuel tanks for the Boeing B-47 jet bomber. To keep weight down, the number of tank components was held to the lowest possible figure. This provided another salient advantage. Fewer components meant less metal thicknesses at joints and less difficulty in joining joints into gas-tight structures. But, the idea also presented some problems in fabrication.

First, how to make the tanks strong enough to meet the terrific demands imposed and yet simple and light in structure? Then, eliminating components, such as conventional quarter-section stampings, meant designing larger sections. This posed problems in getting access for welding. It required greater control of precision because larger sections would magnify slight errors in dimensions. But errors could not be tolerated because all of the big sections were to be joined by butt welds, for simplicity, and the big components must mate perfectly with each other at each joint.

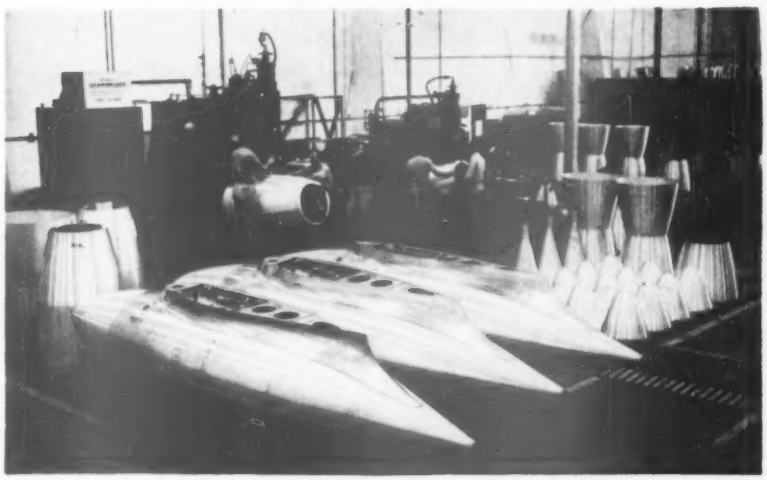

Fig. 1 Glistening Ryan fuel tanks in foreground are strong, light-weight structures achieved through specialized spotwelded design

These problems were solved with a new tank-building technique and design of a structure to fit this method of fabrication. The tremendous B-47 tanks—largest wing tanks built—were designed to consist of a few huge circular sections joined together with lightweight spot welding. Every longitudinal member was eliminated and only simple splice rings used at the welded joints. The big sections were stretched to exact diameter and trimmed to exact length by ingenious machines. Results were extraordinarily successful.

The KC-97 containers consist of only five major circular sections joined with thousands of spot welds. Bolting rings are used on three joints which are designed to separate for shipping purposes. No rivets mar the sleek streamlining of the tank exterior.

There are no longitudinal members in the tank—not even stringers. Only two compact bulkheads are used and these are lightweight riveted assemblies which transmit the loads to the wing fittings. The structure is skin-stressed throughout its envelope of 61S aluminum alloy.

Tank sections consist of a spun nose dome and tail cone, two combination spinnings and welded-sheet sections and a welded-sheet center section. The nose section is a parabolic dome which attaches to the tank with a hexagonal, internal-wrenching bolt. It can be quickly removed to give complete access to the tank interior. The tail cone is a straight-sided cone which is equipped with a soft rubber tip to prevent wing damage in case the tank is inadvertently dropped.

First step in fabricating the big tank component is to roll the 61S alloy into circular sections, which are slightly undersize in diameter, and weld the longitudinal seam on automatic Heliarc machines. The resulting butt weld is almost undetectable because the metal thickness at the joint is the same as that of the parent metal. This provides a seam which can be run through resistance welding machines without special preparation.

After fusion welding the sections are heat treated in a modern elevator-type electric furnace which brings them up to 980° F and quickly cools them by dousing them into a cold water spray. This renders the parts more ductile for forming and stretching.

The components are then stretched to

William Brotherton, Ryan Aeronautical Co., San Diego, Calif.

Fig. 2 Ryan and Boeing representatives inspect an underwing fuel tank attached to a Boeing KC-97 aerial tanker, at Boeing's plant in Seattle

exact diameter on expanding mandrels. Among the largest of their type, these powerful machines employ a huge hydraulic ram which pulls a tapered pin through a segmented set of formed shoes. As the pin drops, the shoes expand, stretching the sections to exact size. Tapers may also be formed by this method. Close precision, to 0.025 in. in diameter, can be obtained without difficulty.

Next step in the process involves the aging of the sections by heating them to 350° F for 8 hr in a gas-fired oven to harden them and raise their yield and ultimate strength to the specified levels.

To trim the tank sections to exact length, another machine is used. The sections are placed on a rotating drum and held firmly in place by air-filled tubes which expand against the inside of the parts. High-speed routers move in and trim the edges of the component as it revolves between them on the drum. Lengths must be cut to 0.010 in. accuracy. Filler cap holes and other openings are blanked out in a hydraulic press. The components are now ready to be spot welded.

Ryan is equipped with a bank of eight huge spot welding machines which are among the nation's largest. These behemoths can make up to 200 spot welds per minute, "fire" 120,000 amp of current and exert 11,000 lb of electrode force. They are capable of handling large sections because the electrode arms are 60 in. long and 62 in. above the floor. They were especially designed for work such as these large tanks.

The tank sections are joined together on these machines with circumferential welds which attach the components to splice rings. Each joint is given two rows of roll spot welds and two rows of seam welds. All work is performed with wheel-type electrodes. Roll spot welds are made on 1.5-in. centers with electrode wheels stopping for each weld in brief, intermittent pauses. Seam welds are a series of overlapping spot welds, made while the wheels are continuously rolling. The result is a strong, gas-tight joint which is extremely light in weight.

Because of its good ductility, 618 aluminum is an excellent alloy for spot welding. However, it must be thoroughly cleaned of all grease, soil and oxides beforehand. Slight amounts of soil and oxide film will alter the electrical resistance of the skins, cause temperature rises and burn-through of the metal.

We developed a superior cleaning agent —RACO 34—which removes all soil and oxides with smooth uniformity. Daily records show that surface resistance is running between 10 and 15 microhms—well below the 50 microhm limits required. Sheets are welded within 24 hr after cleaning because the oxide film will reform within relatively short periods when the metal is exposed to air.

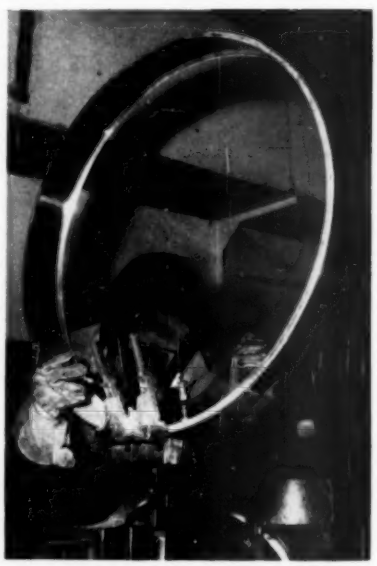

Fig. 3 Tank splice rings are arc welded with inert gas shielded method by Ernest White. These lightweight rings are used to back up the butt welded tank joints which are joined by spotwelding

Each tank section is pressure tested, individually, to show that it is gas-tight. No sealing compounds may be used. In special fixtures, the sections are clamped shut with rubber-faced enclosures and pumped full of compressed air under 4.5 psi pressure. Leak detecting compounds are sprayed on the exterior to disclose the slightest air leak.

When the tank is completely assembled, it is suspended in the air, and pressure tested again. This time it is pumped to 3.5 psi and held under test for 45 min, to reveal slight leaks that would not be evident under limited test periods. A manometer gage is used to show pressure loss. This instrument is so sensitive that it will register a pressure drop of only $^1/_{500}$ lb.

Machined aluminum alloy rings are used at the tank's break-a-way joints. These components must be machined flat on their mating surfaces to within 0.002 in. because they must fit together without more than 0.004-in. spacing at any point. This rigid requirement is insisted upon because the rings must prevent fuel leakage when bolted together even through temperatures range through 250° F.

The rings are drilled on special fixtures. Holes must be located within 0.010 in. accuracy. Rings and bulkheads are drilled at the same time so that perfect coordination exist between them. The rings are spot welded to tank sections using roll spot methods with welds spaced on $^3/_4$-in. centers.

The tanks must be interchangeable in all sections and tank strut fairings must fit all tanks. In one way, the tanks are more critical in dimension than the B-47 tanks which Ryan built. Tank attachment fittings extend over several sections in the KC-97 cell. Since these fittings are precisely spaced because of the wing requirements, this imposes exact tank section lengths and precision-interchangeability upon the fabricated components.

The design permits "nesting" of tank components, one within the other, so that a tank may be shipped in a space of less than half its total volume.

Tanks were placed in steel fixtures and tested with varying amounts of fuel by the application of loads in different directions. The required slosh and vibration tests were conducted at Boeing.

These tests required that the tank be rocked back and forth through a 30-deg angle from 10 to 16 times a minute for 25 hr. At the same time the structure was vibrated at a frequency of 90% of the normal rated crankshaft speed of the KC-97's power plants. Then, the tank was sloshed alone for an additional 15 hr.

Temperature tests require that the tank

be subjected to 160° F for seven days and −65° F for three days while filled with liquid containing a staining agent. Any leakage exhibited by this test or structural failure caused by the slosh and vibration tests will disqualify the design.

Although the tanks are not intended to be dropped in normal use, they are jettisonable for emergency conditions. This is accomplished by attaching them to the underside of the wings with standard bomb attachment fittings. A solenoid switch, actuated by the pilot, cuts the tanks free, like a released bomb. Quick-disconnect fittings for fuel and electrical lines insures their clean break-a-way.

The only accessories within the tank are tandem-mounted, twin electric motor-driven pumps which force the fuel into the aircraft's main fuel system. These units are located in a cavity in the tank bottom and are readily accessible for replacement or repair through a removable access plate.

How Strong Is This Test Frame?

Reproduction of a short article appearing in Vol. 11, No. 3, Steel Construction Digest, highlighting one of the structural steel projects of the Welding Research Council

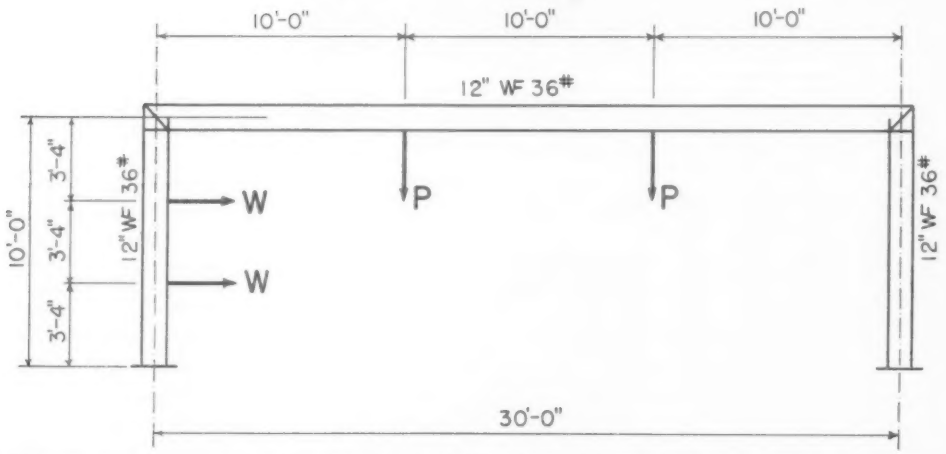

Fig. 1 Dimensions and manner of loading for the full scale rigid-frame load test shown in the photograph

EVERY architect, engineer and builder knows he can depend on structural steel—and thinks he knows just how strong it is. The traditional measure of steel's total strength for construction purposes is the largest load under which it is guaranteed to behave in a truly elastic manner.

Present-day building code provisions based upon this elastic concept are logical and economical for individual beams or those connected flexibly to other members of a steel frame. When members are rigidly connected together, however, the structure as a whole has long been known to possess a much greater load-carrying capacity than indicated by these provisions. The reason is that structural steel has a capacity to yield plastically with no loss of strength, often with some increase in resistance. Hence a rigidly connected, continuous frame can draw upon the reserve of strength in less heavily stressed portions.

Research at Lehigh University sponsored by the American Institute of Steel Construction and other organizations is expected soon to provide the necessary technical data for the use of this more realistic measure of steel's strength—its "ultimate" strength, as revealed by analysis of its plastic rather than its elastic behavior under load. The problem has been to evaluate the magnitude of this extra strength, and how to know for sure the conditions under which it can be relied upon to boost the load-carrying capacity in any given case. The Institute's research project at Lehigh is directed toward finding these answers. Translated into new design techniques, they could produce substantial cost savings and at the same time provide building frames more logically, designed for greater over-all strength.

Figure 2 shows the setup for one of several full-scale rigid-frame load tests which are being run. The dimensions and manner of loading in this test are shown in Fig. 1. Carefully measured loads, indicated by means of arrows in the sketch, are applied by hydraulic jacks, seen in the picture.

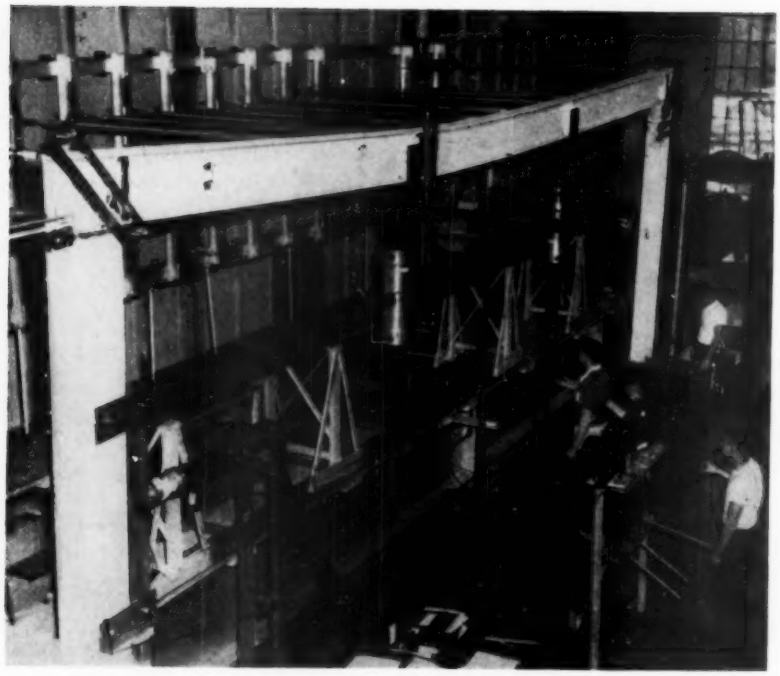

Fig. 2 To find the strength of this 30- x 10-ft test frame, one of several which have been loaded to their "ultimate" strength at Lehigh University, read the accompanying story

The test frame shown was of fully continuous welded construction, 30 by 10 ft, commercially fabricated from 12-in. WF, 36-lb, rolled structural shapes. Plastic analysis of the frame before loading predicted maximum vertical loads, marked *P* on the diagram, of 29,200 lb each, combined with horizontal loads (*W* on the diagram) of 3200 lb simulating wind loading, as measuring the "ultimate" strength of this frame.

Under conventional design practice, using elastic rather than plastic design, the allowable load would have been only 13,000 lb at each point of vertical load. Here are the highlights of the actual test results:

1. The observed "ultimate" strength load was 29,700 lb, within 2% of the predicted maximum.

2. This test load was 2.3 times the load for which the frame could have been designed under present code rules, and 1.5 times the "failure" load indicated by these rules.

3. Until the "ultimate" load had been reached, deflections in the frame were scarcely perceptible to the eye. To further demonstrate the plastic behavior of the steel, however, jacking operations were then continued until the frame had been grossly deformed.

The test is one of an extended series which has been made, the results of all of which have been in close agreement with the predictions. The increase in loads that could be imposed upon the continuous test frames, made possible through plastic behavior, has ranged from 21 to 63% above the "failure" load, based on elastic analysis. In every case the increase was accurately predicted by plastic analysis.

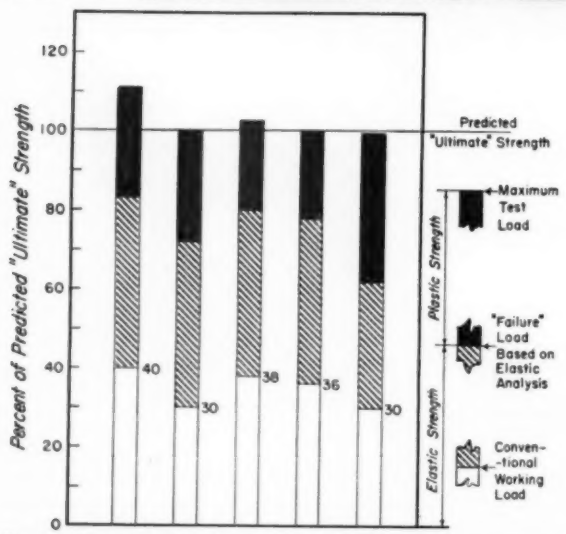

Fig. 3 Results of Lehigh tests, indicating (percentage-wise) relative plastic and elastic strength of five test frames and accuracy of predicted "ultimate" strength as compared with test loads

(*Continued from page 1079*)

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(*Continued on page 1141*)

the AMERICAN WELDING SOCIETY

activities • related events

Executive Committee and Board of Directors Meetings

The second meeting of the 1953–54 Executive Committee and the fourth meeting of the 1953–54 Board of Directors of the AMERICAN WELDING SOCIETY were held on Thursday, Aug. 26, 1954, at 10:00 A.M., and Friday, Aug. 27, 1954, at 10:00 A.M., respectively.

Secretary's Note

The Executive Committee joined in meeting on Thursday, Aug. 26, 1954, for the purpose of: (*a*) reviewing the Agenda for the Board of Directors Meeting; and (*b*) formulating its recommendations for submitting same to the Board of Directors in regard to Agenda items.

Attending the Executive Committee meeting were F. L. Plummer, President AWS, *Chairman;* J. G. Magrath, *Secretary;* F. J. Mooney, *Assistant Secretary* and the following:

Members: J. H. Blankenbuehler, J. J. Chyle, J. H. Humberstone and I. Morrison.

Guests: J. Landau, AWS Auditor.

Attendants at the Board of Directors' meeting were Fred L. Plummer, President AWS, *Chairman;* J. G. Magrath, *Secretary,* and F. J. Mooney, *Assistant Secretary,* and the following members: J. H. Blankenbuehler, J. J. Chyle, J. E. Dato (in part), R. S. Donald, O. B. J. Fraser (in part), D. B. Howard, J. H. Humberstone, C. E. Jackson, I. Morrison, A. E. Pearson, H. W. Pierce (in part), and C. P. Sander.

Financial Report and 1954–55 Budget

Accompanying the minutes of the First Meeting of the Finance Committee held on Aug. 19, 1954, the Finance Committee submitted the proposed budget for the Fiscal Year beginning Sept. 1, 1954, based upon the actual 10 months (to June 30, 1954) operating experience of the 1953–54 Year. The Executive Committee, on August 26th, reviewed the budget and recommended acceptance and approval by the Board of Directors.

Action: Upon motion, duly seconded, the Board of Directors accepted the minutes of the Finance Committee in regard to the budget and approved the proposed budget for the 1954–55 year, as submitted.

Finance Committee Recommendations

Upon motion, duly seconded, the Board of Directors approved: (*a*) the establishment of an Exposition Reserve Fund not exceeding $30,000; (*b*) not less than 20% nor more than 50% of the excess-in-income-over-expenses derived from Spring Exposition activities, subject each year to the Board of Directors' determination of amount, shall be applied toward accumulation of the $30,000 Exposition Reserve Fund, in future years.

Exposition Management

On the basis of recommendation by the Manufacturers Committee, the SOCIETY's Secretary requested authority from the Board of Directors to authorize him to engage the SOCIETY in contract with Exposition Management for the June 1955 Welding Exposition in Kansas City, Mo.

Action: Upon motion, duly seconded, the Board so approved.

Replacement on Board of Directors Membership

As a result of recent elections, C. P. Sander, now Director-at-large and presently serving a full term of three years, will, as of the beginning of November 1954, advance to the Office of 2nd Vice-President. Accordingly, he will be vacating the Office of Director-at-Large on the basis of an unexpired term of two years. It was recommended that the present Board of Directors ballot at this time for the purpose of filling such vacancy. It was pointed out by Mr. Fraser that such ballot would require confirmation by the 1954–55 Board of Directors at its first meeting, in view of the fact that the vacancy actually would not occur until such time. At the Executive Committee meeting on August 26th, President Plummer had appointed a Nominating Committee, consisting of Messrs. Humberstone, Chyle, Morrison and Blankenbuehler, to bring forth nomination recommendations to the Board of Directors. J. H. Humberstone, as Chairman of the Nominating Committee, recommended that Roger W. Clark be nominated to fill the unexpired term of C. P. Sander starting the beginning of November 1954. Mr. Blankenbuehler moved that the nominations be closed.

Action: The nomination of Roger W. Clark to fill the unexpired term of C. P. Sander, effective November 1954, met with unanimous approval, subject to confirmation by the 1954–55 Board of Directors.

Appointment to Technical Council

J. H. Humberstone, Chairman of the Special Committee on Technical Organization and Services, recommended to the Chairman and the Board of Directors that the following persons be appointed to the newly organized Technical Council of the Board of Directors, in the order of approval of such organization by the Board of Directors at its meeting on May 6, 1954: I. Morrison, Chairman, and Roger W. Clark, G. O. Hoglund, J. G. Magrath, Fred L. Plummer and C. P. Sander as Members, and S. A. Greenberg as recording secretary, such being in the order of membership requirement as previously approved.

Action: Upon motion, duly seconded, the Committee personnel for the Technical Council, as recommended, was approved by the Board of Directors and, in the instance of Roger W. Clark, subject to confirmation approval by the 1954–55 Board of Directors.

Ruling on TAC Membership

In view of the organization plan for the Technical Council providing an eventual change in By-Law of the SOCIETY dealing with the appointment by the Board of Directors of three Members-at-Large to the Technical Activities Committee, and inasmuch as the Board has now approved the Technical Council organization and appointed membership thereto, the Secretary recommended that the Board of Directors issue an interim-ruling to the effect that until further notice, the By-Law provision that three members-at-large be appointed annually by the President for a three-year term to the Technical Activities Committee, be suspended.

Action: On motion, duly seconded, the Board of Directors so ruled.

Report by Housing Committee

J. J. Chyle, Chairman of the Special Committee on AWS Housing, submitted a verbal report on the result of a survey-questionnaire sent out to all Members of the 1953–54 Board of Directors regarding their recommendations in connection with AWS Housing, its associations and location. The indications are that a majority do not favor AWS continuing to share the housing with the Engineering Societies, regardless of location; that a majority favor a separate Headquarters Housing for AWS, and that a majority do not favor locating AWS Housing outside the New

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York area. Mr. Chyle stated that his Committee has not had time to prepare formal recommendation to the Board, and that such recommendation would be forthcoming at the November Board of Directors Meeting in Chicago.

Membership Classification

For Dr. G. V. Slottman, Chairman of the Special Committee on Membership Classification, the SOCIETY's Secretary reported that as soon as possible after the 1st of September, the National Headquarters will distribute to Section Officers the recommendations on membership classification made by the Staff of the SOCIETY which are those that have been recommended for distribution by the Chairman of the Special Committee on Membership Classification. The Section Officers shall be requested to return to National Headquarters their opinions regarding adoption of the classifications recommended. (This is in the order of an "Opinion Survey." It is not a vote as there can be no special selection of groups of the SOCIETY's membership body for the purpose of registering official votes.)

Long Island Section

A group of Members in Long Island, having met with the requirements for the establishment of an AWS Section, requested formal authorization to operate as the Long Island AWS Section, with headquarters located in Westbury, L. I.

Action: Upon motion, duly seconded, the formation of the Long Island Section was approved.

Holston Valley Section

A group of Members in Kingsport, Tenn., and Bristol, Va. area, having met with the requirements for the establishment of an AWS Section, requested formal authorization to operate as the Holston Valley Section.

Action: Upon motion, duly seconded, the formation of the Holston Valley Section was approved.

Procedure Clarification

Recently the Board of Directors approved the Section Advisory Committee's recommendation that, in most instances all open territory between two sections, regardless of distance between Section Headquarters cities, should be allocated to one of the Sections. This ruling, an alteration of existing By-Law, could result in delay in starting a new Section if a group in any one particular area, not in or adjacent to the Section Headquarters city, desire to start a Section of their own in an area that had already been assigned to another Section, and would further require the approval of the territory-holding Section as well as the District Director and Section Advisory Committee. The Society's Secretary received clarification from attending Members of the Executive

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Committee on August 26th, including the Chairman of the Section Advisory Committee, on the basis that the headquarters office was always in its rightful position when it proceeded to promote new Sections at any location, regardless of previous territorial assignment.

Manufacturers Committee Chairmanship Appointment

President Fred L. Plummer recommended that T. Pierre Champion, subject to his acceptance, be appointed to the Chairmanship of the Manufacturers Committee.

Action: Upon motion, duly seconded, recommendation was approved.

Appointment to Membership on National Research Council

The Society's Secretary recommended the reappointment of President Fred L. Plummer as AWS Representative Member to the National Research Council, for assignment by that organization to activity on its Division of Engineering for a three-year term from July 1, 1954 to June 30, 1957, as approved and requested by the governing board of the National Academy of Sciences.

Action: Upon motion, duly seconded, reappointment was so approved.

Memorial Resolutions

(*a*) The Secretary presented the Memorial Resolution prepared by a Committee consisting of Prof. Robert A. Wyant and Frank W. Davis for Dr. Wendell F. Hess. (*b*) The Secretary presented the Memorial Resolution prepared by a Committee consisting of H. F. Reinhard and Dr. Harry C. Boardman for Casin W. Obert. (*c*) The Secretary presented the Memorial Resolution prepared by a Committee consisting of Horace F. Henriques and T. C. Fetherston for G. T. Van Alystne.

Action: Upon motion, duly seconded, the foregoing Resolutions, as prepared by the Memorial Resolution Committees, and as amended, were adopted.

Memorial Resolution—H. S. Smith

The Society's Secretary officially advised the Board of Directors of the death of H. S. Smith, Honorary Member of the American Welding Society, very active Member of the Society for many years, a Chairman of the International Acetylene Association's Executive Committee. He recommended that the President be granted the power to appoint a Resolution Committee for presenting an appropriate Memorial Resolution.

Action: Upon motion, duly seconded, the Board of Directors voted that a Resolution Committee consisting of H. F. Reinhard and A. N. Kugler be appointed for preparation of appropriate Memorial Resolution for submission to Mr. Smith's family and organization.

Creation of a Section Charter

A joint recommendation was made by A. E. Pearson, District Director, and the Society's Secretary that a standard Charter be prepared by the Society, such to be in the form of a certificate and to be given to a newly organized Section commemorating its official formation. In discussion, it was agreed that such would be a good practice, and a charter might well be given to Sections that have been organized for a number of years, should they so request. It was also decided by the Board Members in attendance that the text of the charter should confine itself to certification of official formation alone and not to include a listing of the charter Officers or Members. It was recommended that the charter be signed by the Society's President and Secretary.

Action: Upon motion, duly seconded, approval and power was given to the Secretary to proceed with the production and issuance of a standard charter for Sections in the order previously described.

(Secretary's Note: The Secretary advised that he would proceed, but the mechanics of this production would require several months for design, plate work, etc.)

Membership Status Report

The Assistant Secretary reported that as of Aug. 1, 1954, the Society had a

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2. **PROVIDES BETTER WELDS!** Exclusive Arc-Drive Control permits complete penetration on root passes of vertical, overhead, and horizontal welds—prevents "snuff-outs".
3. **SAVES ON POWER!** Lower no-load losses mean savings that can help you pay for the machine in less than 3½ years.
4. **CUTS MAINTENANCE!** Only two basic elements—Transactor* and rectifier. No brushes, armatures or bearings to replace or repair—no moving parts to clean or lubricate. J-21842

FREE BOOK AVAILABLE—Ask your nearest Westinghouse Distributor for a complete-facts book on the new "RA" Welder, or write Westinghouse Electric Corporation, P. O. Box 868, Pittsburgh 30, Pennsylvania.

*Patent applied for

YOU CAN BE SURE...IF IT'S Westinghouse

BECKER

KEEN-ARC CARBONS

FOR CONSISTENTLY GOOD RESULTS

Designed to carry the high currents necessary for intense heat, BBB Keen-Arc Carbons produce a fine-grained weld of high tensile strength. They give a smooth, steady "flowing" flame which does not wander and which is concentrated at the desired focal point. Flame temperature is easily and accurately adjusted by merely changing the ampere input, and heavy copper coating permits gripping at extreme ends—eliminates frequent and periodic resetting.

A COMPLETE LINE OF CARBON WELDING SUPPLIES including carbon arc, graphite electrodes, carbon rods and plates, welding paste, etc. Write for catalog.

BECKER BROTHERS CARBON CO.

3450 South 52nd Ave. Cicero 50, Illinois

LINCOLN "JETWELDING"

...industry's greatest team for low cost, high speed hand welding

Lowest manual welding costs are achieved with "Jetweld", the high speed welding electrode developed and introduced by Lincoln, and "Fleetwelder"—Lincoln's AC transformer-type welder.

For the first time, "Jetweld" utilizes powdered metal in the electrode coating . . . enabling use of higher currents to speed welding. Iron powder becomes an additional source of metal for the weld. Welds are more uniform, free of undercut, of X-ray quality.

THE RESULT: Average increases in speed of 35% . . . *and more.*

CHECK LINCOLN'S NEW LOW PRICES Lincoln's new combination of lower AC machine prices and faster welding with "Jetweld" can cut your welding costs. See for yourself. Have your Lincoln representative check your requirements for electrodes and welders. Call or write.

THE LINCOLN ELECTRIC COMPANY

Dept. 1910

CLEVELAND 17, OHIO

THE WORLD'S LARGEST MANUFACTURER OF ARC WELDING EQUIPMENT

Membership Registration of 10,229, consisting of 9716 active and 513 delinquent Members. The 10,229 registration was compared against membership figures as of the beginning of the Society's Membership Year, Sept. 1, 1953, at which time, the Society had a total registration of 9349, consisting of 9051 active and 298 delinquent members. In addition, he reported that as of Aug. 1, 1954, the Society had an enrollment of 221 Supporting Companies, as compared to 195 as of Sept. 1, 1954.

By-Law Amendments

The Secretary advised that the proposed AWS By-Law Amendments, as submitted to the Membership-at-Large, Apr. 26, 1954, were voted upon by the Membership prior to the closing date of July 25, 1954, in the order following:

Blanket Approval	1089
Blanket Approval with Comments	4
Disapproval in Part	41
Ballots Without Signatures	126
Ballots Improperly Voted or Returned Without Voting	16

The Amendments were released under the old By-Laws which provided that Amendments submitted to the Membership shall be adopted at the expiration of 60 days after mailing the ballots unless rejected by at least 20% of the eligible voting Membership. Accordingly, the By-Law Amendments, as proposed and as submitted to the Membership, were approved and became effective July 26, 1954.

Next Board of Directors Meeting

The President announced that the next meeting of the Board of Directors would take place during the week of the Society's National Fall Meeting in Chicago, Ill., Thursday, Nov. 4, 1954, at 10:00 A.M. in the Hotel Sherman.

Employment Service Bulletin

Services Available

A-659. Welding Technician, Foreman. Sixteen years' experience, including supervisor of manufacturing department; liaison between design engineering and manufacturing departments in new designs, methods and procedures; responsibility for conducting tests of welding methods, techniques and new equipment; instruction and testing of welders for employment and promotions; time studies and planning of assembly sequence. Full knowledge of arc welding, acetylene welding and burning, high-pressure vessel work, submerged arc welding, and use of latest equipment. Familiar with low-chrome alloys, stainless steel, etc. Age 38, married. Will relocate. Résumé and references upon request.

Position Vacant

V-315. Leading manufacturer of arc- and gas-welding equipment offers sales position on a three-level operation; i.e., selling the distributor, working with distributor salesmen and personnel, and selling to industry. Compensation includes excellent salary plus commission over quota, plus expenses; car is furnished.

National Spring Meeting & Welding Exposition

Kansas City, Mo.

June 7–10

Plan to Attend

New Westinghouse ZIP-24 Electrode welds twice as fast— better, easier, too

FASTER WELDS—*50 to 70%* greater deposit rate. And an increase of *30 to 50%* in footage of fillet or lap weld per minute.
BETTER WELDS—balanced fillets give appearance of "machined finish" . . . no undercut with fillet and lap welds . . . perfect wall wash-up . . . easier slag removal . . . minimum spatter loss that reduces cleaning time.
EASIER WELDS—even-edged, smooth, finely rippled weld beads even when handled by an inexperienced operator. Ideal for "drag" technique.
IMMEDIATE DELIVERY—made possible with opening of our second electrode plant . . . a brand-new Westinghouse Electrode Plant at Montevallo, Alabama. To learn more about the remarkable new ZIP-24 just use coupon below.
J-21861

YOU CAN BE SURE...IF IT'S Westinghouse

☐ I would like free samples of the new ZIP-24 electrode.

☐ Send me literature on the complete line of quality electrodes made by Westinghouse (B-6070).

WESTINGHOUSE ELECTRIC CORPORATION
Welding Department
P. O. Box 868, Pittsburgh 30, Pa.

Please send me a free copy of your folder on the ZIP-24 electrode (B-6389).

Name _____
Company _____
Address _____
City _____ State _____

WJ-11-54

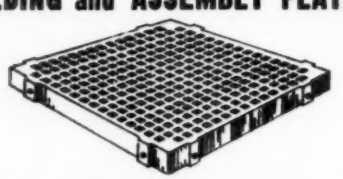
Welding Research Engineer

Opening for metallurgical graduate interested in broad research assignments. Many opportunities for professional advancement including association with recognized technologists and tuition-free graduate study. Assignments cover welding studies in steels and strategic materials such as titanium alloys. Write:

Mr. John F. Collins

ARMOUR RESEARCH FOUNDATION
Illinois Institute of Technology
10 W. 35th Street, Chicago 16, Illinois

WELDING and ASSEMBLY PLATENS



Floor plates — bending tables — layout tables — straightening tables — dog blocks — bending blocks — blacksmith blocks. 5′x5′ — 6′x6′ — 6′x8′ — 6′x10′ — 5′x10′ — 6′x12′ — 3′x3′ — either 6″ or 7″ thick.

STAHL EQUIPMENT CO.
94 Washington St., Brookline Village 46, Mass.

NEWS OF THE INDUSTRY

Alloy Rods Distributors

E. R. Walsh III, General Sales Manager of Alloy Rods Co., announced recently the appointment of the following distributors to the Alloy Rods Co. distributor organization:

District Oxygen Co., Inc., 3801 Bunker Hill Road, Brentwood, Md.
Dow Supply Co., 120 Second Ave., North Grand Forks, N. Dak.
Edmac, Inc., 1015 N. Liberty St., Winston-Salem, N. C.
J. B. Thomas Co., 1902 Church St., Nashville, Tenn.
Welding Equipment Co., 2329 S. Walker, Oklahoma City, Okla.

Weldrod Company Starts Operations

The Weldrod Co., Meadowbrook (Philadelphia Area), Pa., announces the start of operations specializing in the manufacture of Microspooled (precision layer-level wound) inert gas wire for "Aircomatic, Heliarc, sigma and filler arc welding." As originators of the microspooling process, new economies can now be afforded to the industrial consumer.

Also available is Weldrod's new and original line of micro extruded coated arc welding electrodes and cutting rods.

Progressive Acquires Piltz

The Progressive Welder Sales Co., Detroit, nationally prominent supplier of welding equipment, has recently acquired all stock interests of Carl M. Piltz, according to Fred H. Johnson, President of the company.

This acquisition is a part of the company's complete reorganization plan underway at this time.

Foote Mineral Adds Iron Powder

Foote Mineral Co. recently arranged with Easton Metal Powder Co., Inc., to market Easton's iron powder RZ40 to the welding industry on an exclusive basis.

RZ40 is a specially prepared welding electrode coating ingredient of high purity and uniform granulation. For the new iron powder electrodes RZ40 supplies exceptional pure iron in a readily adaptable form.

For detailed information write Foote Mineral Co., Philadelphia 44, Pa.

Western Metal Exposition and Congress

For the ninth time in the last 25 years, the Western Metal Exposition is being prepared by the American Society for Metals on a more extensive scale than ever in the past and with many welding exhibits.

The long-established show will be given March 28th through April 1st (1955) in Pan-Pacific Auditorium, Los Angeles—largest exposition structure west of Chicago.

In addition to the huge auditorium, two specially placed pavilions will be erected to house hundreds of displays by the country's leading firms which produce for or service the west's industrial plants.

William H. Eisenman, ASM's Secretary, says the demand for exhibit space has necessitated using 150,000 sq ft of display area. All will be on the same floor level.

On dates of the show, the Ninth Western Metal Congress will take place in Los Angeles' Ambassador Hotel. Technical programs, round tables and question-answer periods will be conducted by technical societies devoted to the production, treating, machining and fabrication of metals.

Nineteen prominent technical societies besides ASM, Eisenman said, are cosponsoring the exposition and congress. Those belonging to these societies will be registered and admitted to the show on display of membership cards.

Winners of Pandjiris "Positioned Welding Essay Contest" Announced

Winners of the first "Positioned Welding Essay Contest" were recently announced by the sponsor, Pandjiris Weldment Co. of St. Louis.

First prize of $500 was awarded W. B. Wood, Baltimore, Md., for his essay entitled, "Quantity Accompanies Quality in Positioned Welds."

An international flavor was added to the contest by the entry of G. Gordon Musted, Warwickshire, England, who was awarded second prize of $250.

Third prize, $150, went to Roy W. Dryer, Wallington, N. J. Fourth prize, $50, was awarded Robert C. Smith, Pickstown, S. Dak. And Fifth, $50, was won by A. R. Mellini, Cleveland, Ohio.

The five winning essays were selected by a panel of judges made up of industrial leaders: J. J. Powers of the Midvale Co., Philadelphia, Pa.; Howard N. Simms of Black, Sivalls & Bryson, Inc., Oklahoma City, Okla.; and E. J. Hornak of the Sunbeam Corp., Chicago, Ill.

Essays were judged on the basis of (1) monetary value of time saved in the experience described; (2) unique nature of the experience; (3) improved appearance of the product; and (4) cost reduction or increased production resulting from the positioning described.

Purpose of the contest was to stimulate nation-wide educational interest in the use of positioning equipment by industry.

The Pandjiris Weldment Co., manufacturers of welding work positioners, turning rolls and other production welding equipment, has announced they expect to continue the essay contest program with another one in 1955.

Boston Welding Clinic

Latest techniques for welding the "wonder metal" titanium and other quality alloys will be exhibited at the Producers' Welding Show October 13th, 14th and 15th, at the Cambridge, Mass., offices of Whitehead Metal Products Co., Inc.

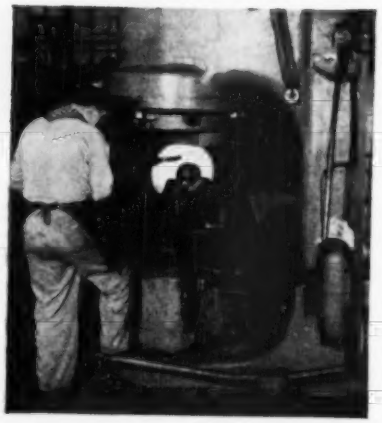
The three-day welding clinic is presented as an industry service to acquaint welders with new welding methods and answer specific welding problems submitted by metal users. The program will include actual demonstrations on metal samples, motion pictures of welding operations, and question-and-answer sessions by welding consultant staffs from such leading producers as the Aluminum Company of America, the American Brass Co., the International Nickel Co., Inc., Air Reduction Sales Co., Air Products Co., Crucible Steel Co., Handy & Harman, Linde Air Products, Lukens Steel Co. and others.

In addition to the welding of titanium, demonstrations will cover aluminum and its alloys, brass and copper alloys, stainless steels, Monel, nickel, Inconel and plastic pipe.

Welding processes will include Aircomatic, Heliarc, consumable electrode inert-gas arc welding, hard surfacing, induction heat, resistance welding, sigma process, torch and arc welding and brazing, cast iron welding, silver brazing and flame cutting.

Invitations to the Producers' Welding Show are available by writing to Whitehead Metal Products Co., Inc., 281 Albany St., Cambridge 39, Mass.

HIGH conductivity forgings & castings for all resistance welding needs..



WE MAKE THEM
OURSELVES....
....WE KNOW
THEY ARE RIGHT

Seam Welding Wheels
Bushings • Knees
Shafts • Spindles • Etc.

Experience and technical skill identify WW ALLOYS. Long a leading reliable source of high conductivity, high strength forgings and castings —in rough or finished form—WW ALLOYS can meet any resistance welding requirement. And with the added advantage of WW's participation in Fansteel's continuous metallurgical research, you know you can count on WW for the job being right every time.

depend on
W.W *for metallurgical know-how and specialized experience*

W545C

WW ALLOYS, INC.
Division of Fansteel Metallurgical Corporation
11644 Cloverdale Ave. • Detroit 4, Michigan

National Welding Supply Assn.

The Central Zone Section of the National Welding Supply Assn. held a meeting at the Hotel Cleveland, September 16–17, with 150 delegates present. There were a number of general addresses, panel discussions and special papers dealing with the problems that confront the National Welding Supply Assn.

The Eastern Zone Group met in Boston, September 20–21 with 159 distributors and manufacturers attending. Special papers dealt with merchandising methods, labor relations, clinics, sales training, profit sharing and advertising.

Salkover Moves to New Plant

Salkover Metal Processing of Illinois, Inc., founded in Chicago in 1941, announces it has moved to its new plant at 2550 Edgington Ave. in Franklin Park, Ill.

Salkover's business is chiefly commercial copper brazing for hundreds of metal product manufacturers in the Chicago area and throughou. the country. In addition to copper brazing the concern also handles bright, scale-free annealing, silver brazing and aluminum brazing. Its new plant was designed and built specifically for its specialized processes and is an outstanding example of modern industrial construction for maximum operating and handling efficiency.

Name Change for Cooper Alloy

The Cooper Alloy Foundry Co., Hillside, N. J., announced recently that in view of continued extension of facilities and services, the name of the company has been changed to Cooper Alloy Corp. Harry A. Cooper, company President, explained that Cooper Alloy is one of the nation's oldest and largest producers of stainless steel cast products, but that for many years it has been manufacturing special product lines in addition to producing custom castings.

In 1931 Cooper Alloy began the production of stainless steel pipe fittings and accessories, and has since grown to be the world's largest producer of these items. In 1945 Cooper Alloy entered the stainless steel valve business, and is already among the top three producers in the country. Welding fittings and a patented line of specialty fittings called Quikupl® have also been added to the line. In 1947 Cooper Alloy became one of the first stainless foundries to produce centrifugally cast stainless steel rings in permanent molds, and since that date has been a leading supplier of these rings for military aircraft. The building of a complete new plant at Clark, N. J., for the purpose of providing high alloy rings, produced in a variety of methods for military aircraft needs, has just been announced.

Stainless Cb for Welded Construction

The American Society of Mechanical Engineers recently approved the use of Carpenter Stainless No. 20 Cb plates, pipe, tubing, bars and forgings in welded construction.

According to a report of the ASME Boiler Code Committee, the alloy may be used in the construction of welded pressure vessels provided certain specifications and fabricating procedures are met. Nine such requirements are set forth in the study, designated ASME Case 1188.

The No. 20 Cb grade has been formulated specifically for those applications which involve welding during fabrication, and for those in which the parts must be placed in service in the as-welded condition. The addition of Columbium to the original Stainless No. 20 alloy minimizes the precipitation of carbides during hot working. This permits use of the welding process in fabricating equipment too large for subsequent annealing.

Stainless No. 20 Cb can be welded by any of the standard electric-arc and resistance-welding processes. Oxy-acetylene welding is not recommended. Sound weld deposits can be obtained by using the welding techniques generally recommended for the fully austenitic steels. No special methods are required.

Typical applications for the alloy include mixing tanks, heat exchangers, process piping, bubble caps, metal cleaning and pickling tanks, spray pickle equipment, etc. The findings of the ASME are expected to further widen the applications for this steel.

Carpenter No. 20 Cb is a relatively new, super corrosion-resistant steel distributed in the forms of sheet, plate, pipe, tubing and bar by the Carpenter Steel Co.'s Alloy Tube Division, Union, N. J.

Welding Awards for Business and Service Establishments

William Ansteth, employed by the Thomas Landscape and Nursery Co. of Tulsa, Okla., has been awarded $1000 as the First Award in a national arc welding competition sponsored by The James F. Lincoln Arc Welding Foundation of Cleveland, Ohio. The award was one of 191 made to persons in nonindustrial business and service establishments who submitted papers describing how welding is or could be used in the operation or maintenance of the business or service. Awards totaling $13,500 were distributed in 43 different states, Alaska and Hawaii.

Mr. Ansteth described the numerous applications of arc welding in the landscape nursery business, showing how substantial monetary savings were made or

Why it pays to specify *The Leading Brand*

YOU SAVE three important ways when you specify TUBE-TURN Welding Fitting and Flanges. (1) You get the most advanced products available . . . assured by Tube Turns' pioneering research. (2) You get the exact fitting and flange for *your* problem promptly because Tube Turns has the world's most complete line . . . available nearby. (3) You get piping ideas through the outstanding Engineering Service and how-to-do-it information of Tube Turns.

These money-saving extras are yours when you specify "TUBE-TURN". Call your nearby Tube Turns' Distributor.

The Leading Manufacturer of Welding Fittings and Flanges

TUBE TURNS LOUISVILLE 1, KENTUCKY

A Division of National Cylinder Gas Company

DISTRICT OFFICES: New York • Philadelphia • Pittsburgh • Cleveland • Chicago • Denver • Los Angeles
San Francisco • Seattle • Atlanta • Tulsa • Houston • Dallas • Midland, Texas

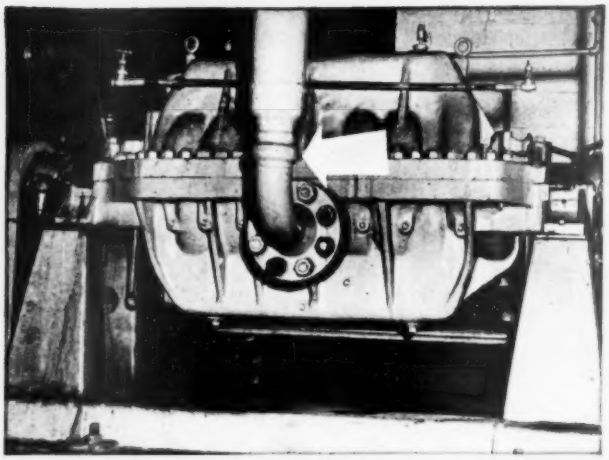
SEE HOW YOU SAVE

TUBE TURNS' *Engineering Service* helps save $50,000

A LARGE PROCESSOR had a problem of retubing seven large cooling units, formerly fabricated from copper tubing and fittings. Thin-walled aluminum was considered as a replacement; however, there were no satisfactory methods for fabricating the metal.

Tube Turns' Engineering Service Division, working with Alcoa, came up with the solution. Special TUBE-TURN Elbows and a new brazing technique were developed and the problem was solved. Total saving by customer was $50,000. This engineering service is one of the extras you get when you do business with the leader . . . Tube Turns.

THE JOB. Brazing TUBE-TURN Aluminum Returns to aluminum tubing for cooling coils.



NEW HYDRAULIC COUPLING. This unique new TUBE-TURN Coupling for hydraulic piping eliminates weld scale and protrusions at joints, protects pumps and other precision equipment for lowest-cost operation. You are sure to get the latest cost-saving developments in fittings and flanges when you specify "TUBE-TURN".

YOU GET COMPLETE SERVICE. Your nearby Tube Turns' Distributor gives you the *exact* answer to your specific piping problem from the world's most complete line of welding fittings and flanges. It includes more than 4000 items . . . in all piping materials, schedules and sizes. This service is backed up by Tube Turns' engineering assistance.

TUBE TURNS, Dept. O-6
224 East Broadway • Louisville 1, Kentucky

Please send free copy of booklet, "Allowable Working Pressures".

Company Name _____

Company Address _____

City _____ *Zone* ____ *State* _____

Your Name _____

Position _____

DISTRICT OFFICES

New York	San Francisco
Philadelphia	Seattle
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Chicago	Tulsa
Denver	Houston
Los Angeles	Dallas
Midland, Texas	

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TUBE TURNS

LOUISVILLE 1, KENTUCKY

A DIVISION OF NATIONAL CYLINDER GAS COMPANY

could have been made by use of this process.

Awards of $500 were made to school employees in State College, Miss., Fort Wayne, Ind. and Phoenix, Ariz., a newspaper employee in Jacksonville, Fla., and a building engineer in Ithaca, N. Y.

The Foundation, according to its Chairman, Dr. E. E. Dreese, sponsored the Program to encourage the study of how the use of arc welding in business and service establishments where it is not normally widely used could contribute to raising the standard of living by reducing the cost of making and distributing goods and services. The Foundation has sponsored similar Programs in the fields of industry, agriculture and engineering education.

Pumps for Lower Neches Valley Authority

A welder places the final bead on discharge nozzle of one of two huge Ingersoll-Rand vertical pumps destined for use by the Lower Neches Valley Authority of Beaumont, Tex. The Lower Neches Valley Authority furnishes fresh water to the vast oil and chemical industries in the Beaumont-Port Arthur area, as well as water for municipal uses in South Jefferson County and irrigation of 60,000 acres of rice fields.

These vertical axial-flow pumps, designated as size 57APS, will each lift 105,000 gpm in the Second Lift Station, transferring water from one canal to another with a total differential head of 17 ft. They will be driven through right-angle gears by Ingersoll-Rand gas engines.

The above-mentioned units will replace obsolete rotary pumps and reciprocating steam engines which had been in service for many years. They will also increase the total pumping capacity of the LNVA from 590,000 to 620,000 gpm.

40-Ton Upper Exhaust Hood Resembles Giant Igloo

Steel struts are welded in a 40-ton cast-iron upper exhaust hood by workman at the General Electric Co.'s turbine factory in Schenectady, N. Y. The struts are welded between steel stubs cast into the iron.

Resembling a giant igloo, the hood is part of the first of six 200,000-kw steam turbine-generators being manufactured for the Ohio Valley Electric Corp.'s new Clifty Creek plant near Madison, Ind. When completed in 1956, this station will be the largest ever built by private enterprise.

The Ohio Valley Electric Corp. was organized by 15 private utilities of the Ohio Valley region to generate electric power for the Atomic Energy Commission's project near Portsmouth, Ohio.

A seventh 200,000-kw turbine-generator has been ordered from G-E for the Ohio Valley Electric Corp.'s second plant at Kyger Creek near Gallipolis, Ohio.

Together the two stations will have a generating capacity of 2,200,000 kw. They will supply the AEC with 1,800,000 kw, the largest block of electric power furnished a single customer in the history of the electrical industry.

Aluminum Barges

The first welded aluminum barges built in this country are being used at the Rockdale (Tex.) works of Aluminum Company of America.

Fabricated entirely of lightweight, corrosion-resistant aluminum, four of these barges were constructed by Butler Manufacturing Co. of Birmingham, Ala. They are 13½ ft long, 7½ ft wide and 2½ ft deep.

Aluminum barges can carry the same dead weight as steel barges at much less draft, and advantage in shallow waters, or

AT THE FRONTIERS OF PROGRESS YOU'LL FIND

AN AIR REDUCTION PRODUCT

FOR WELDING AND CUTTING...

IN THE RED DRUM

HIGHEST QUALITY

DUST-FREE

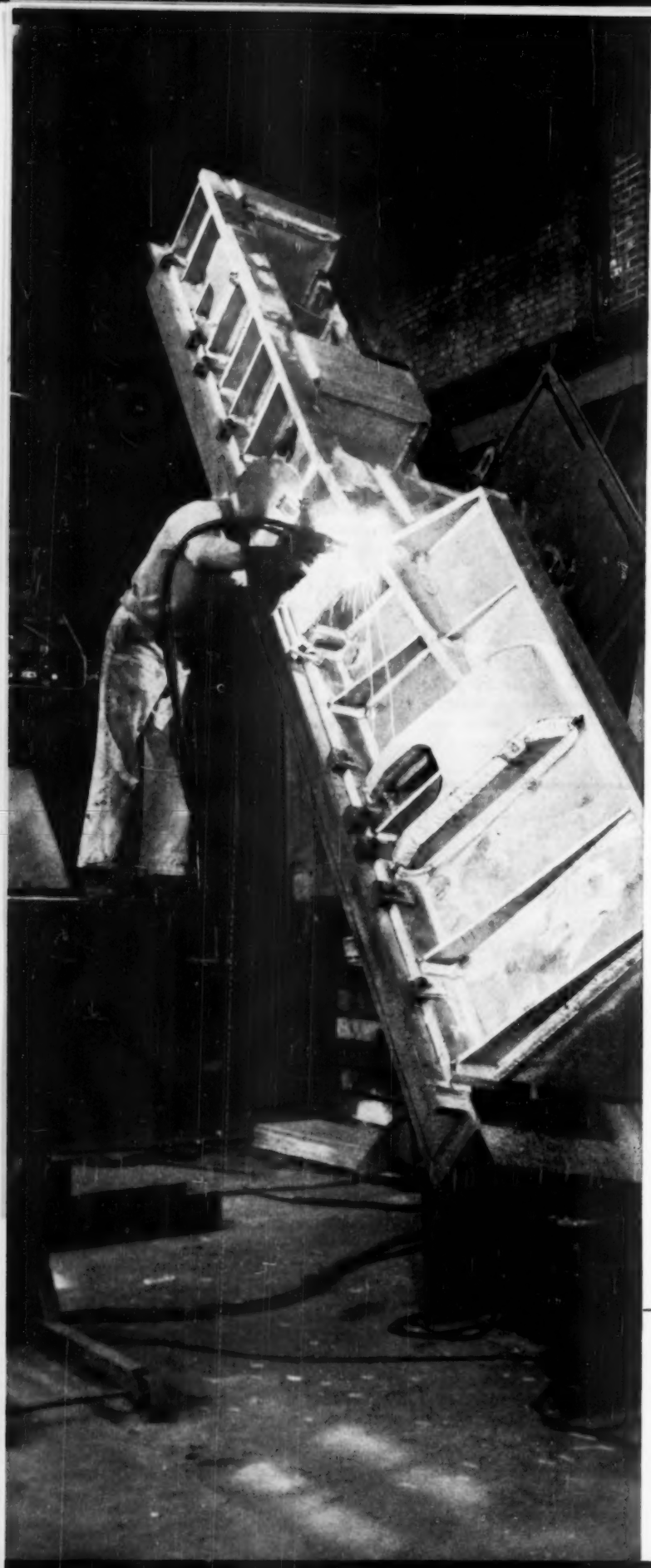
NATIONAL CARBIDE

Write for the name and address of the NATIONAL CARBIDE supplier nearest you.

National Carbide Company

GENERAL OFFICES: 60 EAST 42ND STREET, NEW YORK 17, N. Y.

A DIVISION OF AIR REDUCTION COMPANY, INCORPORATED



Q. Isn't all filler metal for consumable electrode welding about the same?

A. No indeed—it's the quality of the filler metal that controls the quality of the weld.

Q. How can you be sure of getting the best electrode?

A. By insisting on Alcoa's new I.G.* Electrode.

Q. Why?

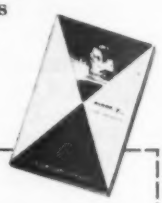
A. Every production lot of Alcoa® I.G. Electrode is test welded and the welds X-rayed for soundness. Each 10-lb spool is individually packed to maintain the super-smooth finish to assure soundness of weld and smoothness of feed. Each of three alloys, 1100, 4043 and 5154 (formerly 2S, 43S and A54S), is available in six diameters. There is no better electrode.

*inert gas

ALCOA ALUMINUM

ALUMINUM COMPANY OF AMERICA

A new, 176-page text, *Welding Alcoa Aluminum*, has just been released by Alcoa. It covers every phase of welding in complete detail. For a free copy, fill in and mail the coupon.



ALUMINUM COMPANY OF AMERICA
930-L Alcoa Bldg.
Pittsburgh 19, Pa.

Gentlemen: Please send book, *Welding Alcoa Aluminum*.

Name _____

Company _____

Title _____

Address _____ City _____ State _____

Q. Where is Alcoa's new I.G. Electrode available?

A. Contact your nearest Alcoa sales office, or, for immediate delivery, call one of the Alcoa Distributors listed below. He carries a complete range of alloys and sizes and you'll find him well qualified to work with your welding experts.

ALBANY, N. Y.
†Eastern Brace-Mueller-Huntley, Inc.

ATLANTA, GA.
†J. M. Tull Metal & Supply Co., Inc.

BALTIMORE, MD.
Whitehead Metal Products Co., Inc.

BIRMINGHAM, ALA.
†Hinkle Supply Co.

BOISE, IDAHO
Pacific Metal Co.

BUFFALO, N. Y.
Brace-Mueller-Huntley, Inc.
Whitehead Metal Products Co., Inc.

CAMBRIDGE, MASS.
Whitehead Metal Products Co., Inc.

CHARLOTTE, N. C.
Edgcomb Steel Co.

CHICAGO, ILL.
†Central Steel & Wire Company
†Steel Sales Corporation
Corey Steel Co.

CINCINNATI, OHIO
Williams & Co., Inc.

CLEVELAND, OHIO
†The Hamilton Steel Co.
Williams & Co., Inc.

COLUMBUS, OHIO
Williams & Co., Inc.

DALLAS, TEXAS
Metal Goods Corp.

DENVER, COLO.
Marsh Steel Corp.
Metal Goods Corp.

DETROIT, MICH.
Central Steel & Wire Co.
Steel Sales Co. of Mich.

HARRISON, N. J.
Whitehead Metal Products Co., Inc.

HOUSTON, TEXAS
Metal Goods Corp.

INDIANAPOLIS, IND.
Steel Sales Co. of Indiana, Inc.

JACKSONVILLE, FLA.
Florida Metals, Inc.

KANSAS CITY, NORTH, MO.
†Marsh Steel Corp.
Metal Goods Corp.

LOS ANGELES, CALIF.
†Ducommun Metals & Supply Co.
Pacific Metals Co., Ltd.

LOUISVILLE, KY.
Williams & Co., Inc.

MIAMI, FLA.
Florida Metals, Inc.

MILFORD, CONN.
†Edgcomb Steel of New England, Inc.

MILWAUKEE, WIS.
Central Steel & Wire Co.
Steel Sales Co. of Wis.

MINNEAPOLIS, MINN.
Steel Sales Co. of Minnesota

NASHUA, N. H.
Edgcomb Steel of New England, Inc.

NEW ORLEANS, LA.
Metal Goods Corp.

NEW YORK, N. Y.
†Whitehead Metal Products Co., Inc.

PHILADELPHIA, PA.
†Edgcomb Steel Co.
Whitehead Metal Products Co., Inc.

PITTSBURGH, PA.
†Williams & Co., Inc.

PORTLAND, ORE.
†Pacific Metal Co.

ROCHESTER, N. Y.
Brace-Mueller-Huntley, Inc.

ST. LOUIS, MO.
†Metal Goods Corp.

SALT LAKE CITY, UTAH
Pacific Metals Co., Ltd.

SAN DIEGO, CALIF.
Ducommun Metals & Supply Co.

SAN FRANCISCO, CALIF.
†Pacific Metals Co., Ltd.

SEATTLE, WASH.
Pacific Metal Co.

SYRACUSE, N. Y.
†Brace-Mueller-Huntley, Inc.
Whitehead Metal Products Co., Inc.

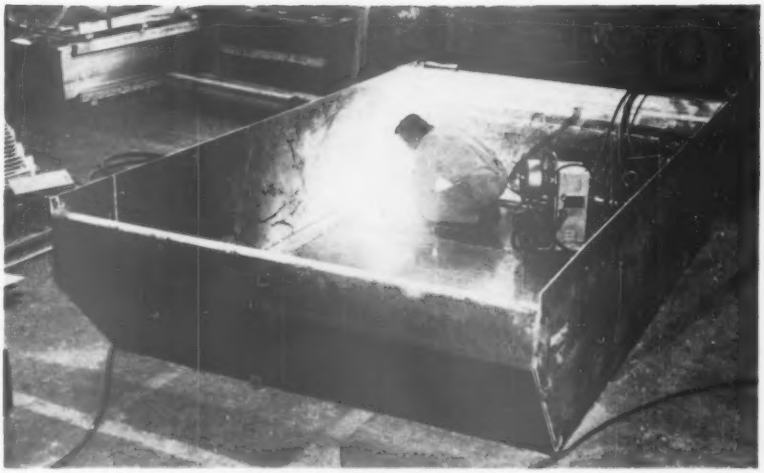
TAMPA, FLA.
†Florida Metals, Inc.

TOLEDO, OHIO
Williams & Co., Inc.

TULSA, OKLA.
Metal Goods Corp.

YORK, PA.
Edgcomb Steel Co.

†Home Office



carry a greater dead weight at the same draft as a steel barge of the same dimensions.

The natural resistance to corrosion possessed by aluminum is of importance to a barge operator. Painting of aluminum barges is unnecessary in most waters, but, of course, when used in salt water a good antifouling paint is recommended.

Two of the barges are constructed of Alcoa 61S-T6 aluminum alloy of all-welded design. The other two barges are of new Alcoa nonheat-treatable marine alloys; one of A54S and the other of XC56S. Welding was done with the new Alcoa IG electrode at an average rate of about 24 ipm.

By using the consumable electrode procedures it was possible to assemble this barge design with its flat surfaces in aluminum construction with no more distortion than would be experienced with a comparable steel barge.

These aluminum barges are being used to support pumps which remove water from cuts where fuel has been extracted for Alcoa's smelting operations.

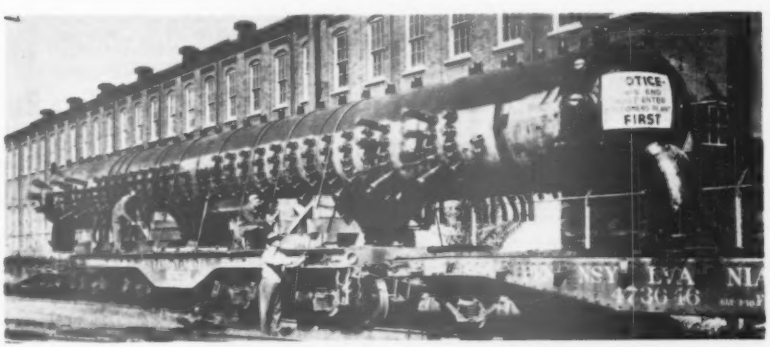
Steam Drum

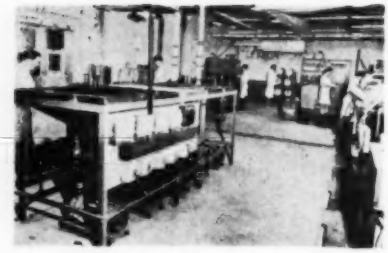
A 193-ton steam drum, built by the Babcock & Wilcox Co. at its Barberton, Ohio, plant, required three railroad flat cars for its trip to Detroit this week. The drum will become part of the largest single boiler ever built in the world. The giant B & W boiler, which will reach 15 stories in height when erected, will supply steam for the first of two turbine generating units to be installed at the River Rouge power plant of the Detroit Edison Co. Each unit will have a maximum capacity of 273,000 kw, enough electricity to supply the needs of over 700,000 people.

Babcock & Wilcox engineers believe the drum to be one of the most unusual welded pressure vessels ever built. It is 82 ft long and the thickness of its steel walls measures almost 6 in. The B & W boiler itself will consume more than 400,000 tons of coal each year—enough to heat 50,000 average homes—but will have greatly increased efficiency. It will produce a maximum of 1,720,000 lb of steam per hour and deliver it to the turbine at a pressure of 2000 psi per square inch.

New Research Lab

Research and product development in chemical cleaning took another giant step on September 15th, when Oakite Products, Inc., threw open the doors to their new and expanded laboratories at 350 Hudson St., New York City. An extensive look into the future of industrial cleaning materials and methods was the feature of the afternoon.





John A. Carter, president of the concern, and J. J. Basch, manager of research and product development, were on hand to welcome the visitors. Mr. Carter, in a brief opening talk, stated that, "Our 45-year history has been a constant research and service program, and these new facilities will enable us to make even greater strides in furnishing modern industry with the products it demands."

Guests and officials of the company took part in the opening ceremony and then toured the laboratories, which cover 30 000 sq ft on a single floor.

The laboratory, which is subdivided into three major sections—product development, customer service and engineering—will continue to devote itself to the cleaning and sanitation problems of the nation's production and service industries. In addition, a complete pilot plant is planned for the small scale manufacture of detergents and solvents prior to extensive field testing.

Student Engineers Receive Design Awards

Richard Seikaly of Syracuse, N. Y., has achieved top honors among engineering undergraduate students by winning the First Grand Award in a national design competition sponsored by The James F. Lincoln Arc Welding Foundation of Cleveland, Ohio. The award, which was accompanied by a check for $1250, was made for a paper presenting a comparison study of a band saw frame to demonstrate the advantage of welded design over cast iron design. The Foundation also presented Syracuse University, where Seikaly made the study, $1000 for four annual scholarships honoring Seikaly.

The award was one of 46 made by the Foundation to 62 engineering students representing 25 different engineering colleges. The Foundation sponsors a design competition annually among engineering undergraduates for welded designs of machinery or machine parts and welded structures or structural parts. In addition to the national recognition, the competition offers $5000 to students and $1750 to schools in scholarship funds.

Seikaly's paper presented his analysis of the design and manufacturing of a band saw frame both for cast iron and welded steel. The welded steel design proved to be $2^1/_2$ times lighter and $3^1/_3$ times less expensive than the cast iron design.

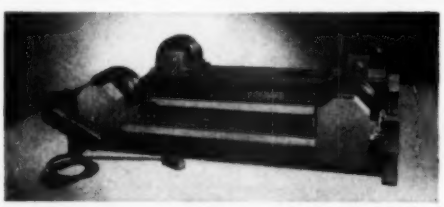
The Second Grand Award of $1000 was shared by Richard Tannenbaum and Jay Gang, students at New York University. They made a comparison study of a bolted vs. a welded billboard sign. Their study showed a 27% cost advantage for welding. Their school received $500 for scholarships in their honor.

The Third Grand Award of $500 went to David Honnold at the University of Oklahoma which received $250 in his honor.

Papers were judged by a Jury of Award composed of Dr. E. E. Dreese, Chairman, Department of Electrical Engineering, Ohio State University; Dean James H. Sams, School of Engineering, Clemson Agricultural College and Professor Sidney Shore, School of Civil Engineering, University of Pennsylvania.

The Foundation is sponsoring the eighth annual competition of the Award Program for 1954–55. All engineering undergraduates are eligible, and the Rules may be obtained from The James F. Lincoln Arc Welding Foundation, Cleveland 17, Ohio.

Aronson QUALITY POSITIONING EQUIPMENT



PRECISION BUILT GEAR DRIVEN POSITIONERS with Magnetic Brakes
500 lbs. to 24,000 lbs. capacity

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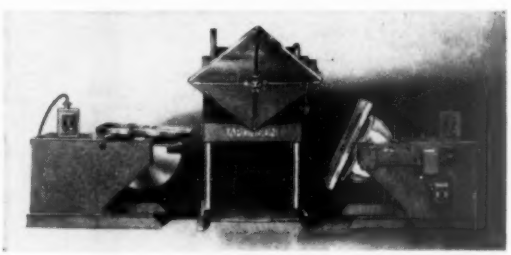
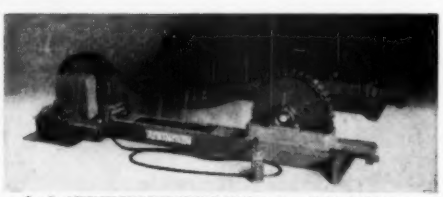
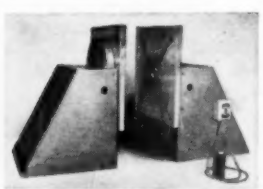
5,000 lbs. to 150 tons capacity





TracTred ENDLESS BELT TYPE TURNING ROLLS for thin walled tank fabrication
6,000 lbs. to 18,000 lbs. capacity



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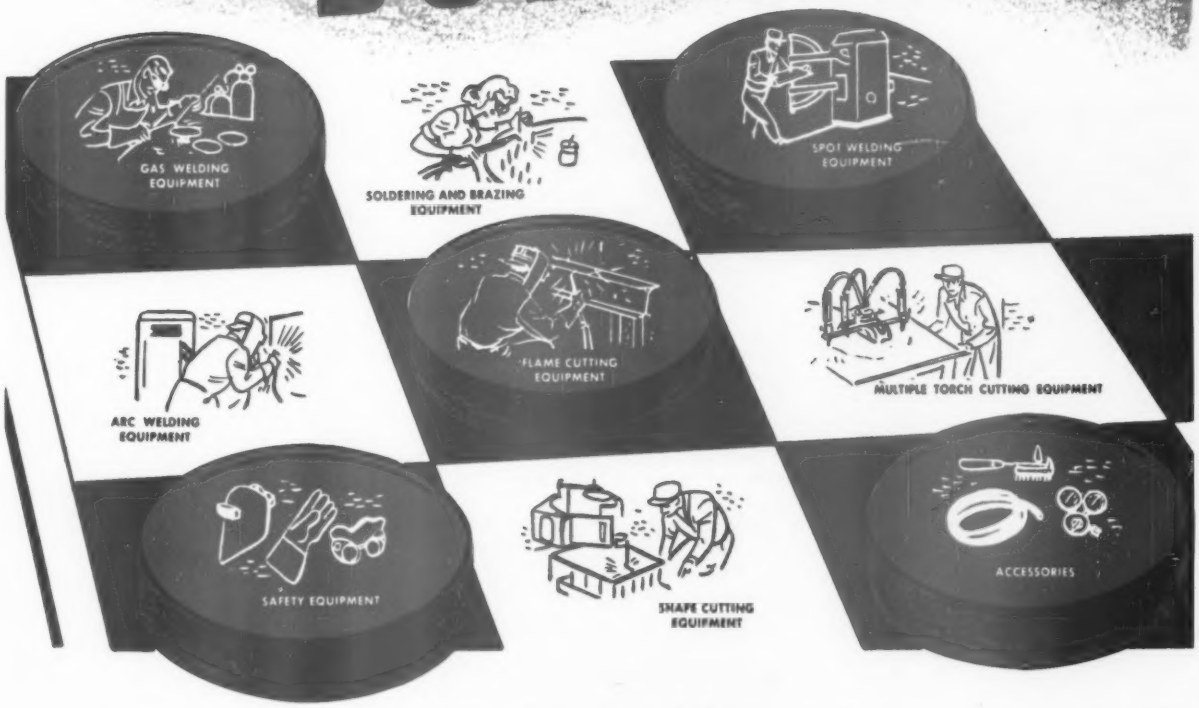

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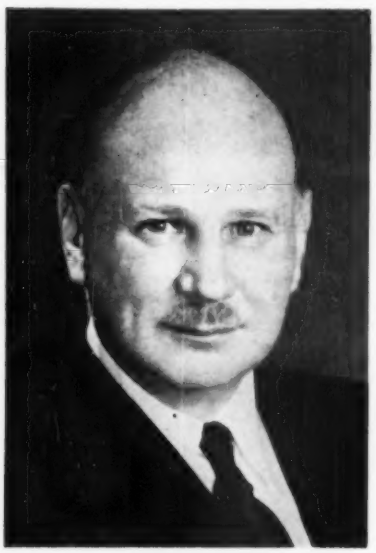
PERSONNEL

Howard Biers Elected President of IIW

Howard Biers who was appointed *Vice-President* of the International Institute of Welding and one of the three American representatives on the council of the IIW has been elected President of IIW. The following biography is reproduced from Who's Who in America.



Biers, Howard (William Richard) cons. engr., b. N. Y. C., Dec. 3, 1904; s. Arthur and Anne Eleanor (Zoll) B.; B.S., U. Va., 1925; S.M., Mass. Inst. Tech., 1927; Dr. mont., Montanistische Hochschule Leoben, Austria 1954; m. Constance Lucie Mary Herzog, Nov. 10, 1937; 1 son, William Richard. Research metallurgist Union Carbide & Carbon Research Labs., Inc., N. Y. C., 1927–30; cons. engr., Paris, Brussels, London, 1930–40; tech. adv. to metals controller; tech. adv. com. mem. Dept. Munitions and Supply, Canadian Govt., 1940–48; also Canadian chmn. ferro alloy com. with W. P. B.; mem. (for Can.) Joint U. S.–United Kingdom Canadian Metall. Mission; spl. adv. Dept. Trade and Commerce; cons. engr. Union Carbide & Carbon Corp., N. Y. C., since 1948. Fellow Inst. of Metallurgists (Eng.); President Internat. Inst. Welding; mem. Commn. Permanente Internationale de l'Acetylene (Paris) (U. S. del.) Inst. of Metals (Eng.), Iron and Steel Inst. (Eng.), Am. Welding Soc. (Chmn. com. Internat. Inst. Welding), Am. Inst. of Mining and Metallurgical Engineers, Am. Soc. for Metals, Verein Deutscher Eisenhuettenleute (Germany), Instituto del Hierro y del Acero (Spain), Associacao Brasileira de Metais (Brazil), Eisenhuette Oesterreich (Austria). Recipient citation Internat. Conf. World Metall. Congress, 1951; Foreign Operations Admin. U. S. A., 1953. Clubs: Univ., The Pilgrims (N. Y. C.); The Travellers (Paris, France); The Pilgrims (London, Eng.). Contrb. Tech. publs. Home: French Farm, Silvermine, Norwalk, Conn. Office: 30 E. 42nd St., N. Y. C. 17.

J. P. Coughlin Joins Eutectic

Rene D. Wasserman, President of Eutectic Welding Alloys Corp., Flushing, announced that J. P. Coughlin, former Manager of the Arc Welding Division of Westinghouse Electric Co. in Buffalo, has joined Eutectic as Assistant to the President.

Mr. Coughlin is well known in the welding industry through his association with the Westinghouse Co. during the last 15 years where he was in charge of welding, sales-engineering and production. He was with the Navy Department during the war and his expert knowledge of weld construction was a distinct contribution to the war effort. He rose from the rank of Lieutenant to Lt. Commander and returned to his civilian occupation in November 1945.

"The increasing demand for Eutectic's products throughout the world," stated Rene D. Wasserman, "makes the appointment necessary. His wide administrative experience and his familiarity with problems of production and distribution will be fully utilized and will be exceedingly helpful in strengthening and enlarging our ever-growing distributor organization."

Mr. Coughlin is married and has four children. He was educated at the University of Missouri and holds a B.S. degree in electrical engineering. He is a member of the National Sales Executive Club of Buffalo, N. Y. (Frontier Division), the American Welding Society and the National Welding Supply Assn.

Glen C. Flumerfelt Appointed Regional Manager

Glen C. Flumerfelt, 31 Rio Vista Ave., Oakland, Calif., has been appointed Regional Manager for All-State Welding Alloys Co., Inc., to cover the states of California, Nevada, Utah, Colorado, New Mexico and Arizona and the Hawaiian Islands. Announcement of his appointment was made by T. D. Nast, president, at the Company's offices, White Plains, N. Y.

Mr. Flumerfelt will be responsible for sales and service to the users and distributors of All-State Alloys and Fluxes in his area. He will be the contact man for All-State's metallurgical resources and experimental laboratory which provide technical assistance for the solution of all manner of problems having to do with the welding, brazing, soldering, tinning and cutting of metals for maintenance, reclamation, construction and production of metal parts and equipment.

A member of the American Welding Society and in the construction field and welding industry for 18 years, Mr. Flumerfelt's ability to serve the western states is further enhanced by the fact that during World War II he served overseas (England and Germany) in a civilian capacity

with the Army Educational Branch as welding engineer and instructor. He taught electric and acetylene welding for private welding schools and was with the California State Dept. of Education for approximately five years.

Just previous to this appointment Mr. Flumerfelt had headquartered in the Northwestern area where he had had comparable responsibilities in selling and servicing All-State Alloys and Fluxes.

Welding Section Head

George R. Pease has been appointed head of the Welding Section of the Bayonne Research Laboratory of the International Nickel Co., Inc.

Mr. Pease is a Master of Science graduate of the University of Massachusetts. He was a chemist with the Springfield Armory of the U. S. Army Ordinance Department from 1939 until 1945, when he joined the staff of the Bayonne Laboratory. His work has centered on the development of new welding electrodes and has resulted in a number of patents and technical papers in this field. He is a member of the American Society for Metals, the AMERICAN WELDING SOCIETY and the American Chemical Society.

Caluwaert Made Manager

John P. Caluwaert has just been appointed manager of Industrial Air Products of the South, according to an announcement by Gilbert Schnitzer, President of the firm.

Industrial Air Products of the South has its main office at 907 S. Peters in New Orleans, and one of the largest oxygen plants in the south on the Industrial canal in New Orleans. This firm also has a plant at Pascagoula, Miss., and a branch at Baton Rouge, La., with dealers in major cities of the south.

Mr. Caluwaert replaces the late G. H. Jeudevine. With Industrial Air Products since 1944, Caluwaert was assistant sales manager of the Portland office before his appointment in the south.

He began his career in welding with Smith Welding Equipment Co. of Minneapolis in 1930, and served with them until 1936.

Since that time Caluwaert has worked as a welding specialist for J. E. Haseltine Co., Field Engineer for Stoody Corp. and Factory Engineer for Jackson Products.

Because of his past experience and knowledge of the welding industry, Mr. Caluwaert will be of vital service to the many friends and customers of this southern operation.

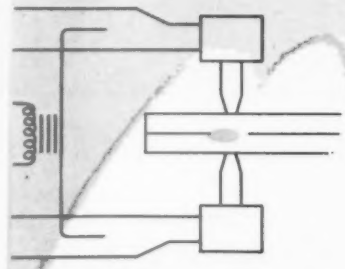
persistent hoarseness or cough

...is one of the seven commonest danger signals that *may* mean cancer...but should *always* mean a visit to your doctor.

The other six danger signals are —Any sore that does not heal ...A lump or thickening in the breast or elsewhere...Unusual bleeding or discharge...Any change in a wart or mole... Persistent indigestion or difficulty in swallowing...Any change in normal bowel habits.

For other facts about cancer that may some day save your life, phone the American Cancer Society office nearest you, or write to "Cancer"—in care of your local Post Office.

American Cancer Society®



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Use AMPCO WELD* Spot-welding Tips

Get tough with your spot-welding costs. Reduce downtime, get longer runs — use Ampco Weld spot-welding tips. These electrodes have unusual resistance to mushrooming and wear. They have high electrical conductivity, too — they stay cooler, don't stick to the work.

That's why Ampco Weld resistance-welding tips require far fewer dressings, stay on the job longer, save you money because of fewer shutdowns.

Ampco-weld tips are part of an extensive line of Ampco resistance-welding products. All meet, and most exceed, RWMA specifications. And free, expert Ampco engineering service is available if you want or need it. Order Ampco tips today — and step up your production for lower costs.

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NEW LITERATURE

Manifolds

National Welding Equipment Co., 218 Fremont St., San Francisco 5, Calif., has issued looseleaf technical notes on cylinder manifolds—their design, construction and usefulness. Copy available on request.

Hobart Arc Welding News

Volume XI, No. 3, "Hobart Arc Welding News," a 24-page booklet of interesting photographs and articles on welding from all over North America, is now available. Copies are mailed free of charge to anyone interested in arc welding.

Many of the articles feature time and money saving applications. To get your copy, write the Hobart Brothers Co., Troy, Ohio.

Welding Problems

"How to Overcome Your Welding Problems and Improve Your Welding Techniques" is the title of a new 8-page, fully illustrated booklet, just released by Eutectic Welding Alloys Corp., Flushing, N. Y.

Written for welders by welders it is a practical down-to-earth manual designed for those who wish to increase their welding knowledge and improve their skill. Metallurgists and welding engineers also will find this booklet very useful because they will gain an insight into the many problems facing the maintenance welder.

The many variables the maintenance welder has to face are mentioned and explained; the causes of stress raisers, starter cracks and hard spots are discussed and methods indicated whereby they may be avoided or minimized.

The section on design shows by means of illustrations how weld failures can be avoided if due consideration is given to joint design to allow for multiaxial stresses.

Many of these problems are eliminated if the correct welding techniques are used. Drawings indicate how a simple change in joint design will reduce these dangers of weld failure.

The practical welding experience of many years is packed into this 8-page booklet and should be of inestimable value to the welder who takes a pride in his work and wants to know some of the answers to everyday problems.

Copies may be had free of charge by writing to Eutectic Welding Alloys Corp., 40-40 172nd St., Flushing 58, L. I., N. Y.

Resistance Welding

Sciaky Bros., Inc., 4915 W. 67th St., Chicago, Ill., Vol. 3, No. 10, *Resistance Welding at Work*, covering case history of Sciaky electric resistance welding equipment as used at Lockwood Manufacturing Co, Cincinnati, Ohio, manufacturers of commercial baking pans.

This well-illustrated, four-page bulletin describes another interesting example of metal fabrication with resistance welding. Lockwood's needs are readily satisfied with Sciaky patented three-phase resistance welding. Not only is the cost of fabrication reduced, but stronger and more sanitary bread pans are produced. Through the use of Sciaky patented three-phase welding, Lockwood maintains their high reputation for an excellent product. They are able to place a three-year adjusted Service Warranty on all bread pans.

Industrial Products Catalog

American Brake Shoe Co., well known in the transportation industry for over 50 years, has published its first complete catalog, with emphasis on industrial products. This 48-page, two-color book illustrates representative parts produced by 11 divisions of the company and details physical properties or characteristics where pertinent. Breakdown is by type of product, and the catalog contains sections on ferrous castings, nonferrous castings, bearing materials, sintered metals, steel forgings, welding products, air compressors, industrial pumps, dredge pumps and railroad products.

Ductile Iron

Effect of Chemistry and Section Size on Properties of Ductile Iron—Bull. DI-16, 12 pages, 24 charts and photomicrographs—discusses the influence of carbon, silicon and phosphorous contents and of section size on the mechanical properties of ductile iron, a recently developed family of irons which possesses the process advantages of cast iron and which has engineering properties comparable to those of cast steel. A wide range of properties may be secured by variations in heat treatment and composition. High levels of properties are obtainable in heavy as well as light sections. International Nickel Co., New York 5, N. Y.

Strength and Resistance of Metals

This book, by John M. Lessells, consists of 450 pages.

The aim of this volume on the strength and resistance of metals is to provide the senior and graduate student as well as the design engineer with information on the behavior of metals under stress as it has been revealed by numerous workers in this field. Mention is made of nonferrous alloys and cast iron in those particular instances where their behavior differs from that of steel. The subject matter also refers to the elements of physical metallurgy, and a knowledge of this will help the engineer and metallurgist to appreciate each other's work.

Published by John Wiley & Sons, Inc., 440 Fourth Ave., New York 16, N. Y., the book sells for $10.

Materials of Construction

Written by M. O. Withey and G. W. Washa, this book presents the latest data concerning the sources, manufacture and fabrication of the principal materials of construction. It gives carefully selected information covering their more important mechanical and physical properties, and the influence of various factors on these properties. Causes of defects and variations, and how they may be discovered, are discussed in simple terms.

Completely up to date, the work reviews the most recently determined methods and the latest statistical data. New testing machines and strain-measuring devices, for example, are fully covered, along with an entire section on nondestructive tests. Included is the latest information on chemical seasoning, wood products and timber connectors. New types of cement testing methods are given, and a full chapter is devoted to concrete aggregates, including data on new testing methods and lightweight aggregates. The latest mix design methods and techniques for checking proportions of fresh and hardened concrete are offered—plus information on new concrete products.

Also included is the best available knowledge on wrought iron, alloy steels, nonferrous metals, fatigue of materials, effect of mechanical work on the properties of steel, heat treatment of steel, and the effects of temperature on the properties of metals.

The book is well illustrated, and a large

Valve seat inserts, brazed with REXWELD 64, shown as they leave the furnace. Processed by the Ferrotherm Company for Thompson Products.

REXWELD

for uniform brazing of high-temperature parts use REXWELD 64 brazing powder...

Every lot of Crucible REXWELD 64 brazing powder gives *uniform* high quality. Because of the close melting control used in REXWELD 64 production, the flow point remains constant and furnace temperatures can be maintained.

REXWELD 64 brazing powder, which conforms to AMS 4775, is a nickel-chromium-boron alloy powder designed for furnace brazing of stainless steel and high-temperature alloys. It was chosen by the Ferrotherm Company, Cleveland, Ohio, to join the two sections of the aircraft valve seat shown above. For REXWELD 64 provides good oxidation and corrosion resistance—and higher strength at both high and low temperatures.

When you have a brazing application involving parts for high temperature service, you can't make a better choice than REXWELD 64 brazing powder. For prompt delivery—or further data on REXWELD 64—*call Crucible.*

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WELDERS' GROUND CLAMPS
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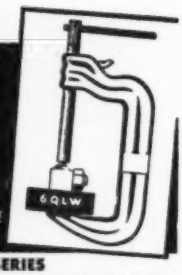

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ASH SERIES
8 SIZES
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number of references to additional sources of information has been included.

The book is published by John Wiley & Sons, Inc., 440 Fourth Ave., New York 16, N. Y. Price $9.00.

Safety Booklet

"Disaster," a new booklet designed to assist industrial plants in choosing safety and rescue equipment for use in emergencies has just been published by Mine Safety Appliances Co. of Pittsburgh, Pa.

While the booklet makes no attempt to outline the organization of a disaster-control plant, it does assume the existence of groups or teams within a plant whose job it is to effect rescue and recovery operations in case of tornadoes, fires, explosions or other types of emergencies. It is published to show just what items of equipment are needed by these teams.

Separated into five sections, the MSA booklet discusses the equipment needs of a rescue team, an engineering team, and a medical team, and contains a table-of-equipment check-chart for quick reference. It also has a catalog description and picture of each item recommended.

Copies of the booklet, Number 5000-4, can be obtained from Mine Safety Appliances Co., 201 North Braddock Ave., Pittsburgh 8, Pa.

Pipe Making

How the continuous weld process of pipe making was developed is told briefly in a new illustrated leaflet of the Fretz-Moon Tube Co., Inc., Butler, Pa.

The leaflet was prepared for visitors to the company's plant, which is one of the world's largest producers of small diameter steel pipe. Fretz-Moon, in 1921, originated the continuous weld process, which has since been adopted for use in the world's largest and most modern pipe mills, producing steel pipe in a wide variety of sizes.

Pipe is produced in a long, continuous piece, and then cut automatically to uniform lengths. By the older butt weld method, one piece of pipe was made at a time, its length being limited by the length of the furnace.

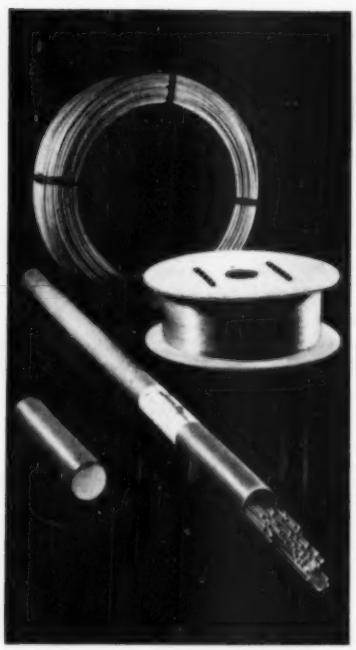
The Fretz-Moon plant has a capacity of 60,000 tons, or about 50,000 miles of pipe, a year. The company operates as a "pipe makers' pipe maker," a major portion of its output going to other pipe manufacturers for sale through their normal channels of distribution. Since 1946, Fretz-Moon has produced only what is known to the trade as $^1/_8$-, $^1/_4$- and $^3/_8$-in. pipe.

Much of the $^1/_4$- and $^3/_8$-in. pipe is used as lance or burnout pipe, feeding oxygen into open-hearth and blast furnaces for tapping the heats; the pipe melts the instant it enters the furnace, but not before it serves its purpose of supplying the necessary oxygen. Fretz-Moon Pipe also is used in the production of equipment for the oil and gas, automotive, and railroad industries, for farming, for temperature control and air-conditioning systems, and wherever else a small diameter pipe is required

Welding of Stainless Tubular Products

A folder of interest and value to those confronted with the task of welding stainless tubular products has been published by the Tubular Products Division of the Babcock & Wilcox Co. This informative folder outlines the welding characteristics of both austenitic and ferritic grades of stainless steels and discusses the question of proper joint design. In addition, it briefly describes various welding methods such as: fusion, metallic-arc, oxy-acetylene, atomic-hydrogen, inert-gas arc and flash-butt welding. A table of recommended arc welding procedures is included which suggests electrodes, preheating and postwelding heat treatment.

Copies of this new folder, known as TDC 162, are available free upon request to the division's general sales offices at Beaver Falls, Pa.



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use
Drawalloy
CORPORATION
QUALITY CONTROLLED STAINLESS STEEL WELDING WIRE

▶ available in Spools, Coils and Lengths
▶ for Automatic and Semi-Automatic gas and inert arc welding

Welding wire by Drawalloy is made to an exacting high standard . . . a high standard established by long-experienced welding men who know welding wire and how it must work. Set-ups for automatic and semi-automatic welding take more time than regular welding. That's why you want to be sure of satisfaction *before* you start. This *reliable* wire is weld-tested to give the best results every time. Write today for complete information and prices.

WELDING SUPPLY DISTRIBUTORS: Get prompt delivery on all sizes and grades of stainless and tool steel wire for gas and inert arc welding.

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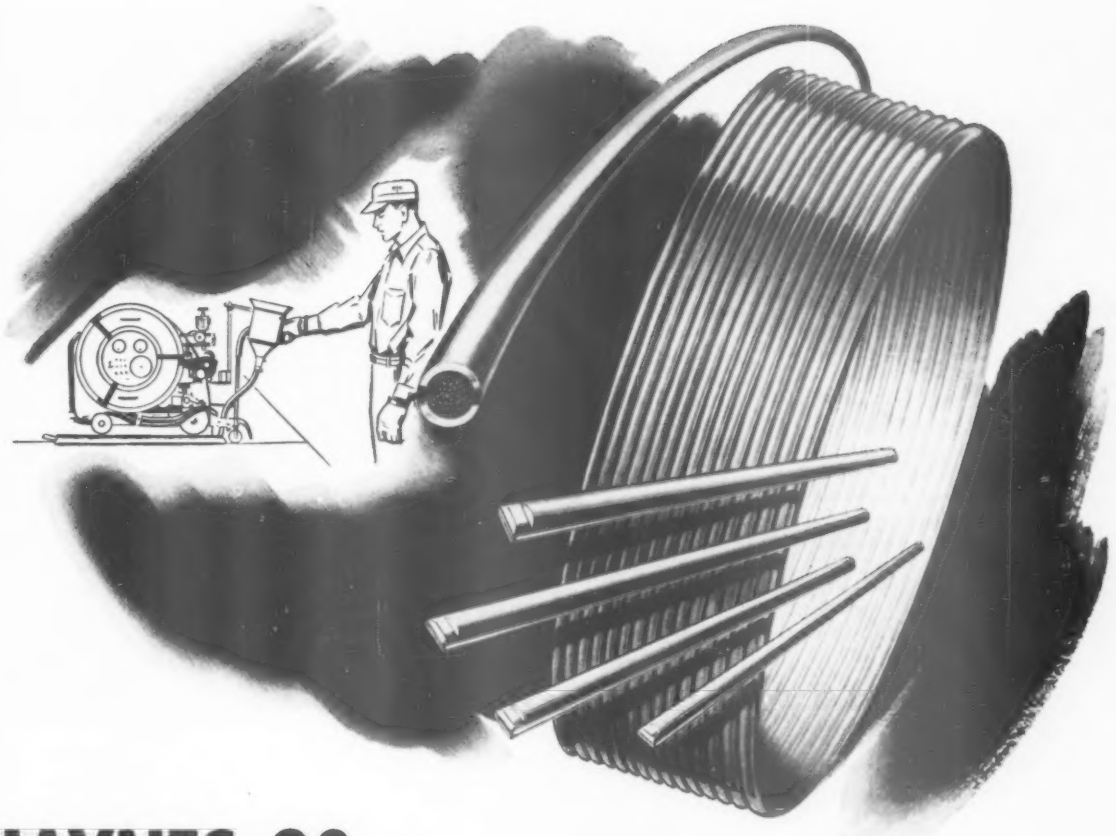


HAYNES 90 alloy now available in tubes and at a MUCH LOWER PRICE

TRADE-MARK

Now you can save even more by hard-facing wearing-parts with HAYNES 90 alloy. This is because HAYNES 90 costs so much less in this new economical tube form. These new tube rods produce sound, uniform deposits that won't crumble or flake off at temperatures up to 1000°F. They provide the same high abrasion, impact, and corrosion resistance—the same dependable protection for your equipment that HAYNES 90 brought to you as a cast rod—and at a much lower price.

For manual hard-facing, HAYNES 90 tube rod comes in convenient 14-in. lengths for easy application with standard metallic-arc welding equipment. For rapid coating of large parts, HAYNES 90 also comes in coils for mechanized hard-facing by the submerged-arc, inert gas, and open-arc methods.

HAYNES 93, HASCROME, and HAYSTELLITE alloys are also available in this economical tube rod form. HAYNES 93 iron-base rod is noted for high abrasion and corrosion resistance . . . HASCROME iron-base rod for high impact resistance . . . and HAYSTELLITE tungsten carbide rod is tops for resistance to severe abrasion.

Your local dealer carries a complete line of HAYNES hard-facing rods. Contact him for complete details. If you don't know the location of your local dealer, write to Haynes Stellite Company, a Division of Union Carbide and Carbon Corporation, Kokomo, Indiana.

See... | Your local Haynes Stellite Dealer

— or —

Write... | to Haynes Stellite Company

"Haynes," "Hascrome," "Haystellite," are registered trade-marks of Union Carbide and Carbon Corporation.

NEW PRODUCTS

Spark Protection

Permatex Form-A-Gasket Number 2, which is a gasket paste used widely for threaded, flanged, hose and other connections to prevent seepage or leakage of various liquids, can be adopted to the welding trade.

The application of this product over the top and sole stitches of shoes, prevents welding electrode sparks from igniting the stitches, and "fusing" through the leather, and consequently burning the welder's foot. It also prevents the stitches from coming out, and either ruining the shoe or requiring restitching.

Permatex Co., Inc., 1702–1720 Avenue Y, Brooklyn 35, New York.

Jetweld 2 HT

A new electrode for welding high-tensile strength joints has been announced by the Lincoln Electric Co. of Cleveland, Ohio. The new electrode, called Jetweld 2 HT, contains iron powder in the coating which gives it the advantages of speed and performance associated with this new type electrode. This new electrode, the company says, represents another addition to its line of iron powder electrodes designed for making specified types of joints rather than as all-purpose electrodes.

Jetweld 2 HT is especially designed for high-speed welding of flat fillet and deep groove joints where high tensile strength and low crack sensitivity are required. It will give the lowest cost per foot of X-ray quality weld and has extremely easy slag removal, top physical properties and smooth appearance.

Jetweld 2 HT, it is claimed, will weld some butt and deep groove joints as much as 50% faster than conventional electrodes. It operates on either a-c or d-c, with a-c operation preferred. The bead has excellent wash-in. Slag removal is also excellent. Ease of operation and low fatigue factor result from the drag technique used with heavy coatings. Spatter is reduced to a minimum, and the smooth appearance on cover pass welds meets automatic welding standards.

The properties of Jetweld 2 HT recommend it for work requiring high tensile strength and for X-ray work. Typical stress relieved values for the new electrode are tensile 76,700 psi, yield 64,300 psi, elongation 27%. Jetweld 2 HT is classified as E-7020 and is available in $^{7}/_{32}$ in. diam.

Hard-Surfacing Electrode

A new hard-surfacing electrode announced by All-State Welding Alloys Co., Inc., 249–55 Ferris Ave., White Plains, N. Y., becomes almost twice as hard and consequently about twice as wear-resistant when the equipment on which it is applied is put to work. Deposits from this electrode work-harden to 48–50 Rockwell C.

This new electrode is an alloy of chromium, manganese and nickel and is applicable with a-c or d-c welders. It is particularly designed for both abrasion and impact wear resistance on manganese steels and high-carbon steels. It can be used as a base for hard-facing alloys, or can be applied directly as a work-hardenable, wear-resistant overlay. Its as-welded properties include: Tensile strength 125,000 psi, elongation in 2 in. 19% and hardness 26–32 Rockwell C.

It is now available through all All-State Distributors in $^{1}/_{8}$-, $^{5}/_{32}$- and $^{3}/_{16}$- in. core sizes. Standard packages are 5 lb.

Spot-Welder Head

A new precision spot-welder head, especially designed for welding delicate and small parts in ferrous, nonferrous or dissimilar metals has been developed by Ewald Instruments, 93 Nassau St., New York 38, N. Y. The Ewald Model 4 Spot Welder Head is designed for use with miniature parts from $^{1}/_{8}$ to 0.0005 in. combined thickness and is applicable to precision welding of very small parts usually considered difficult to weld.

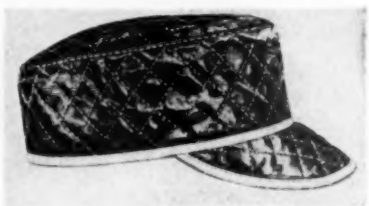
To permit very short welding cycles and obtain reliable follow-up forging action, the weight of the moving parts of the Ewald Spot Welder Head has been reduced to less than 4 oz, which is one of the unique characteristics of this precision spot welder.

Welding pressure can be adjusted from 6 oz to 15 lb, another unusual feature according to the manufacturer, the pressure being indicated on a calibrated scale. The control switch triggers the power supply when the preset pressure is reached, regardless of material thickness and the setting of the electrodes.

The welder has ample space for the mounting of fixtures and may be used with any power supply and timer combination, or the Ewald "stored energy" power plants which are designed for use with this welder. The Model 4 Spot Welder Head is suited for experimental or production work on radio tube parts transistors, potentiometers and a wide variety of other miniaturized components. Information may be obtained from the manufacturer at the address given above.

Cap

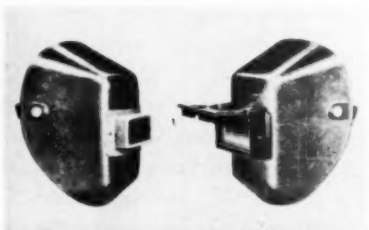
The danger of stray sparks igniting a welder's cap is now sharply reduced due to a cap now being made of fire-retardant fabric. The cap resembles the conventional soft quilted black welder's cap in



most respects. However, the fire retardant fabric is quilted with red nylon extra strength thread plus a red binding at the bottom edge. The cap is now being sold at welder's supply houses and work clothing stores at approximately $1.00 each. The cap is manufactured by the Louisville Cap Co., Louisville, Ky.

Welding Helmets

These new fiber-glass welding helmets, made in a fixed front and a lift-front type, feature the narrow front shape popular with many welders.



The real economy of fiber-glass welding helmets was proved by two earlier Jackson models. Resistant to heat and moisture, they withstand weld spatter, hold their shape and outlast helmets of vulcanized fiber.

Headgears are of smooth, easy to clean plastic with cork-padded sweatband. Friction pivots may be adjusted by hand while helmet is in use to hold it in any position or make it float down without jolting stops.

Made by Jackson Products, Inc., Warren, Mich.

Cap and Helmet

This new line of fiber-glass safety hats meets general industrial requirements and Federal Specifications for electrical resistance, impact and penetration by falling objects, flammability and moisture absorption.

The full-rimmed hat, 14 oz, and the cap (with visor only, 12¼ oz) are made in gray, white, yellow, green and brown. A combination safety cap and arc-welding helmet is offered with either fixed or lift-front lens holder.

An exclusive feature is a headband of extruded plastic with replaceable, cork-lined sweatband, said to hold its shape better and give firmer fit. Straps of webbing form the usual cradle on top of the head.

Bulletin and price list on request.

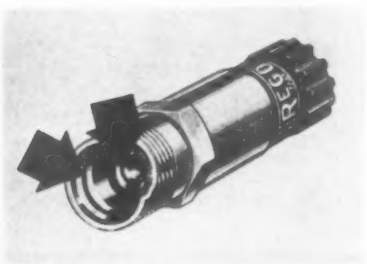
Made by Jackson Products, Inc., Warren, Mich.

Neoprene Welding Torch Seating Rings

An improved model of the popular Rego "GX" welding torch, the original model of which was introduced 21 years ago, features Neoprene seating rings in the seating surfaces of the torch head.

According to National Cylinder Gas Co., Chicago, this new type seating, as contrasted to conventional metal-to-metal seating, permits the user to make a gas-tight connection of the mixer with the torch by hand tightening, thus facilitating the changing of tips. It also allows hand-coupling of the Rego "KXA" cutting attachment to convert the welding torch to a cutting torch.

The new type seating, it is said, will make the Rego "GX" torch especially suitable for use in sandy or dusty atmospheres, or for work requiring frequent changes from welding to cutting, and vice versa, using the same torch handle for both operations.



Arrows point to seating rings

Portable Flame Cutting Machine

The Burdett Oxygen Co. of Cleveland, Ohio, announces its new portable cutting machine with performance equal to many stationary types. The entire machine weighs only 16 lb, making it easy to handle while at the same time reducing operator fatigue.

It will cut in a straight line or bevel; cut circles and shapes with equal ease on metal ranging from ⅛ to 4 in. Power is by means of 110-v, 60-cycle motor.

Other features are ease and simplicity of operation and maintenance.

Descriptive literature and prices may be obtained by writing to the Burdett Oxygen Co., 3300 Lakeside Ave., Cleveland 14, Ohio.

Low-Maintenance, A-C Welders

A complete new industrial line of low maintenance, a-c welders for high speed production has been announced by the General Electric Co.'s Welding Department.

Consisting of NEMA rated 300-, 400- and 500-amp models, the line is now in production at the department's York, Pa., plant where the newest techniques for welder manufacture and quality control are used, according to company engineers.

The new G-E welders feature stepless current control, silicone insulation and aluminum coil windings, and are equipped with a large current scale, which the operator can read from a considerable distance without returning to the welder.

FREE

valuable help in improving welding results, cutting welding costs. From "America's Leading Institution Devoted To The Research and Manufacture of Specialized Metal-Joining Alloys."

Low Temperature EUTECTIC WELDING ALLOYS

TIS 2241
MANUAL OF DESIGN AND WELDING ENGINEERING. 20-page digest of most useful, popular book Eutectic ever offered. Basic welding "know-how." Valuable to engineers, foremen, weldors. Suggests new, profitable uses for welding. Shows successful designs possible with "non-fusion" welding. Covers steel welding, all metals. Hints on weld inspection and control.

TIS 1010
TRUCK AND CAR FLEET MAINTENANCE MANUAL. 56 pages of pictures and text giving practical welding help on "101" automotive jobs. Tells how to salvage castings, repair shafts, transmissions, clutch and drive assemblies. Describes frame and spring repairs. Invaluable for saving time, money on body repairs and for wear resistant overlays. Every industrial, repair shop and garage weldor needs this book.

TIS 800
TOOL & DIE SALVAGE MANUAL. 68 pages of effective tool and die welding and salvage recommendations. Includes basic welding procedures. Covers cast iron, kirksite and shoe or "clicker" dies. Has useful tempering chart and fully develops unique ability of "non-fusion" welding to eliminate damage to base metals. Special sections on new methods of production welding tools, producing forgeable welds. Hundreds of hints for cutting tool costs.

All Eutectic literature is prepared from actual case histories showing how Eutectic Low Temperature Welding Alloys solve welding problems and make possible substantial savings which can not be realized with conventional materials.

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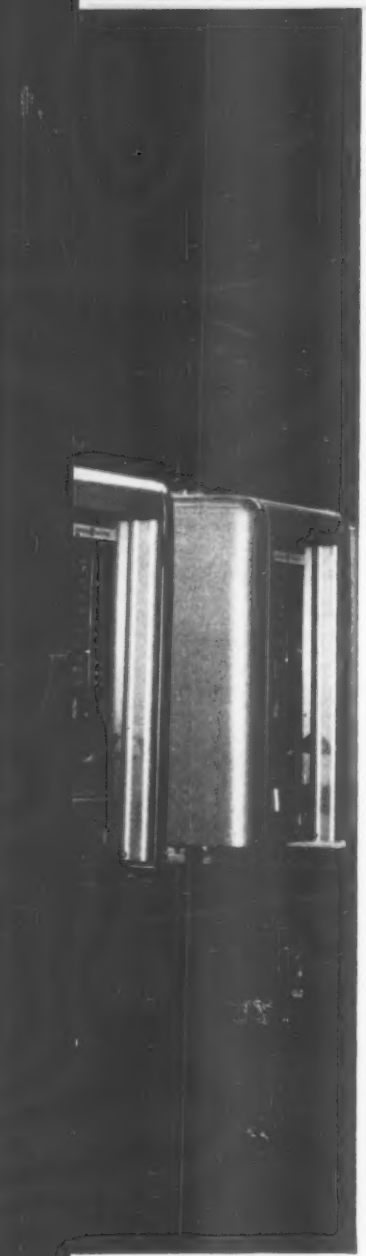
WELDING ARCS NEWS

LONG-LIFE WELDERS ASSURED BY MODERN TESTING AT NEW G-E PLANT

NEW, EXPANDED G-E PLANT at York, Pa., devotes modern manufacturing-testing-facilities to new standards in arc-welding equipment. New G-E a-c welders undergo extensive quality controls . . .

PERFORMANCE TESTING of new a-c welder backs up long-life span. Every phase of operation is checked before the welder is released for production.



G.E. announces a NEW line of long-life a-c transformer welders

life-span increased with new design and materials—backed up by exacting quality controls, including 20 life-tests, 21 inspections

From its new, expanded manufacturing plant at York, Pa., General Electric now brings you industry's most modern line of a-c transformer welders. Available in 300-, 400-, and 500-amp models for indoor and outdoor service—this new equipment features improved design, new materials, and will give long, dependable service.

MOVING-COIL DESIGN REDUCES WEAR and vibration normal to other types of welder design. No moving magnetic parts are used. This low-maintenance design also furnishes accurate pinpointing of current settings for real welding precision.

IMPROVED AUTOMATIC "HOT-START" gives you the advantage of instant arc striking throughout the current range. Now you can be sure of quick starts even at the low end of the range.

PROTECTION AGAINST OVERHEATING DAMAGE caused by short-time overloads is provided by silicone insulation on aluminum-wound coils. This insulator stands up against overload temperatures which disintegrate ordinary varnishes.

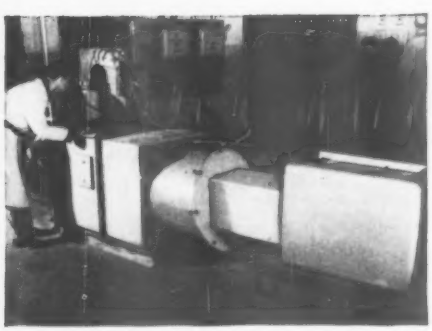
NEW ALUMINUM WINDINGS are used on transformer coils for reliable service, even under highly critical conditions.

REMOVABLE SIDE COVERS, OPEN CONSTRUCTION speed servicing. Each side cover of the G-E a-c welder can be removed quickly by simply taking out three screws.

EVEN QUIETER OPERATION than normal for a-c welders is achieved by a special vibration-insulating mount for coil supports. The moving primary coil virtually "floats," with a minimum of friction, vibration, and fatiguing noise.

IMPROVING ON EASY-TO-HANDLE ARC inherent to a-c equipment, the new G-E transformer welder provides arc stabilizing capacitors. The result is an unusually steady arc, which can be manipulated easily for faster travel speeds and increased production.

ASK YOUR G-E WELDING DISTRIBUTOR today for bulletin GEA-6243 for full details on the new long-life a-c welders. You will find his name and address in the yellow pages of your phone book, and on the next page. See this new G-E welder at the National Metals Exposition at Chicago in November. General Electric Co., Schenectady, N. Y. 710-20



PROPER COOLING OF EQUIPMENT, essential for dependable service, is checked in air-flow test, which determines the amount of air moving through welder.

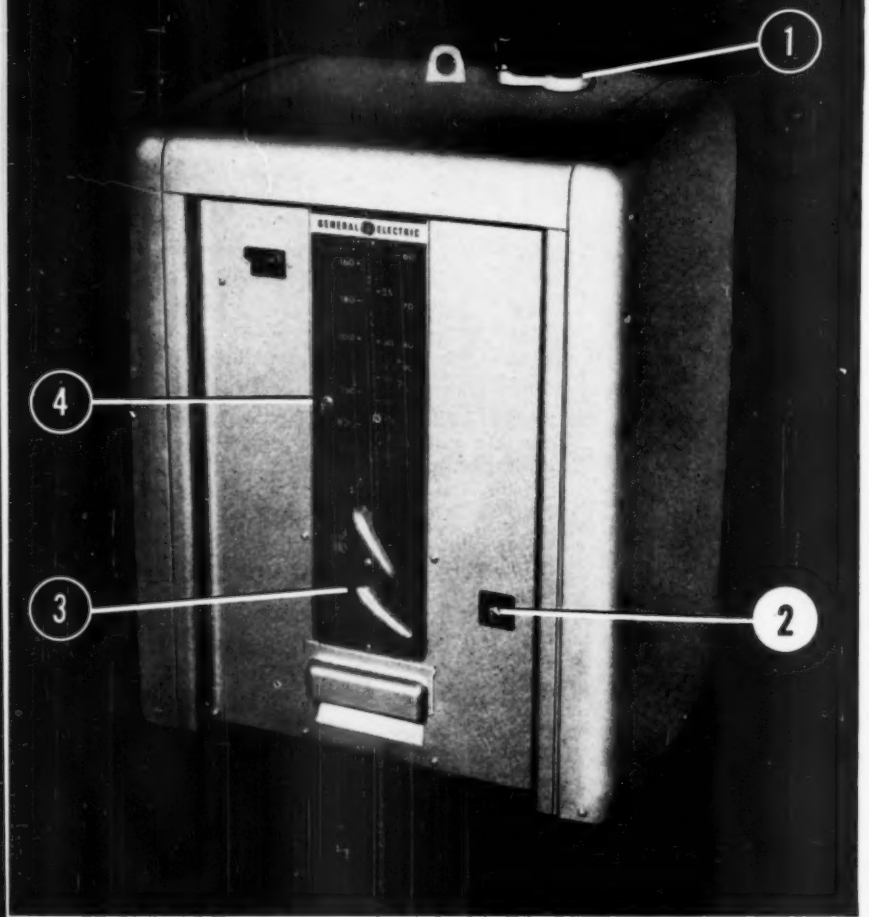
SIX TIMES RATED VOLTAGE is applied to primary coil for fifteen seconds, in testing of new a-c welder. This test assures high safety factor for silicone insulation which is used on all coil windings.

	General Electric	Company A	Company B	Company C
Moving Coil Design	✓			✓
Aluminum Windings	✓	✓		
Silicone Insulation	✓			
Hot Start	✓	✓	✓	
Arc-Stabilizing Capacitors	✓	✓		
Large Logarithmic Current Scale	✓			✓

COMPARISON WITH OTHER MAKES shows you that the new G-E a-c welder is your best buy. For more G-E welding news, turn page . . .

GENERAL ELECTRIC

NEW G-E 300-amp rectifier welder offers full-time arc-force control



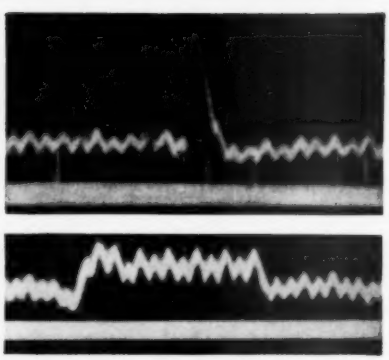
NEW G-E 300-amp rectifier welder offers *full-time* arc-force control . . . and many improved features, including—

1 **FINGERTIP-ADJUSTABLE MOVING COIL:** provides stepless current control for pinpoint accuracy, reduced wear and vibration. Current range is wide—20–375 amps.

2 **ARC-FORCE CONTROL IS FULL TIME:** doesn't cut out in the middle of an electrode. High arc force blasts away short-circuiting metal droplets, which ordinarily freeze short arcs. Results—unusual arc stability, deep penetration, and easy maneuvering.

3 **REVERSING POLARITY SWITCH:** conveniently mounted for instant changing from straight to reverse polarity.

4 **SPRING-LOADED CLEANOUT SWITCH** reverses ventilating fan for blowing dust off rectifiers—prolongs equipment life. Write your G-E welding distributor for bulletin GEA-6242.



SUSTAINED SHORT CIRCUIT occurs without arc-force control (lower oscillogram). Metal cools, electrode freezes. With *full-time* G-E arc-force control (upper), metal is blasted away, short circuit is brief.

Contact Your G-E Welding Distributor Today

Alabama: Birmingham—Alabama Oxygen, Young & Vann Supply; Mobile—Turner Supply

Arizona: Phoenix—Consolidated Welding Supply

California: Fresno, Los Angeles, Oakland, Sacramento, San Diego, San Francisco, Ventura—Victor Equipment

Colorado: Boulder, Colorado Springs, Denver, Durango, Ft. Collins, Ft. Morgan, Greeley, LaJunta, Longmont, Pueblo, Sterling—Hendrie & Bolthoff

Connecticut: Hartford, New Haven—Harris Company

Florida: Hollywood—Florida Gas & Chemical

Georgia: Athens—Welding Gas Products; Atlanta, Macon—Welding Supply & Serivce; Augusta—Marks Oxygen; Columbus—Williams Welding Supplies; Gainesville—Welding Gas Products

Idaho: Boise—Gate City Steel

Illinois: Chicago, Moline, Morton, Rockford—Machinery & Welder

Indiana: Evansville—Drillmaster Supply; Ft. Wayne, Indianapolis—Sutton-Garten; South Bend—Perry Welding Sales & Service

Iowa: Des Moines—Machinery & Welder

Kansas: Wichita—Standard Products

Kentucky: Louisville—Reliable Welding; Paducah—Henry A. Petter Supply

Louisiana: Alexandria, Shreveport—Hughes Oxygen; New Orleans—Consolidated Welding Supplies

Maryland: Baltimore—Arcway Equipment

Massachusetts: Boston—New England G-E Welding Sales Division

Michigan: Detroit—Welding Sales & Engineering; Grand Rapids—Miller Welding Supply

Minnesota: Duluth—W.P.&R.S. Mars; St. Paul—Production Materials

Mississippi: Jackson—Jackson Welding & Supply

Missouri: Kansas City—Hohenschild Welders Supply; St. Louis—Machinery & Welder

Montana: Billings—Valley Welders Supply; Billings, Bozeman, Cut Bank, Glasgow, Great Falls, Havre, Kalispell, Miles City, Shelby, Sidney, Whitefish—Valley Motor Supply; Butte, Great Falls—Montana Hardware

Nebraska: Lincoln—Lincoln Welding & Supply; Omaha—Baum Iron

New Jersey: Kenilworth—Welding Sales Corp.

New Mexico: Albuquerque—Industrial Supply Co.; Hobbs—Western Oxygen; Las Cruces, Silver City—Car Parts Depot, Inc.

New York: Buffalo—Welding Equipment Sales; New York—Welding Sales Corp.; Syracuse—Welding Engineering & Equipment

North Carolina: Charlotte—Dixie Gases; Gastonia—Gastonia Motor Parts

North Dakota: Bismarck, Fargo—Acme Welding Supply

Ohio: Akron, Cincinnati, Cleveland, Columbus, Dayton, Mansfield—Burdett Oxygen; Toledo—Odland Iron Works

Oklahoma: Oklahoma City—Hooper Supply; Tulsa—G-E Welding Sales Division

Oregon: Eugene, Portland—J. E. Haseltine; Medford, Portland—Industrial Air Products

Pennsylvania: Allentown, Philadelphia, Pittsburgh—Arcway Equipment

South Carolina: Columbia, Greenville—Welding Gas Products

South Dakota: Deadwood—Hendrie & Bolthoff

Tennessee: Chattanooga, Knoxville, Nashville—Welding Gas Products; Memphis—Delta Oxygen

Texas: Abilene—M&M Welding Supply; Alice, Corpus Christi—Crane Welding Supply; Alpine, El Paso, Marfa, Pecos—Car Parts Depot; Amarillo—Welding Equipment & Supply; Beaumont—H. & W. Welding Supply; Brownsville, Harlingen—Acetylene Oxygen; Dallas—Hill Equipment & Supply; Houston—G-E Welding Sales Division; Lubbock—Welders Supply of Lubbock; Midland—West Texas Welders Supply; Odessa, Pecos—Western Oxygen; Orange—Marine & Petroleum Supply; Pecos—Welding Supply Co.; Plainview—Plains Welding Supply; San Angelo—Southwestern Welding Supply; Texarkana—Hughes Oxygen; Wichita Falls—McGinnis Welding Supply

Utah: Salt Lake City—The Galigher Co.

Washington: Seattle, Spokane— J. E. Haseltine; Seattle, Spokane, Yakima—Industrial Air Products

West Virginia: Bluefield—Bluefield Supply; Charleston—Virginian Electric; Huntington, Logan—Logan Hardware & Supply

Wisconsin: Milwaukee—Machinery & Welder

Wyoming: Cody, Lovell—Valley Motor Supply Co.

Alaska: Anchorage—Northern Supply

Canada: Toronto—Canadian G.E.

Hawaii: Honolulu—American Factors, Ltd.

GENERAL ELECTRIC

Fingertip current adjustment and quieter operation are made possible by a new feature in which the coil supports float in a special rubber bushing.

Highly accurate current settings may be obtained since the welders utilize the moving primary coil design and a large current scale which has wider calibrations in the lower ranges. Current adjustments are accomplished by raising and lowering the primary coil. Simplicity of this design results in a minimum of vibration and mechanical wear and easier maintenance, the engineers said.

Wide current ranges on the new welders (38–375 on 300 amp, 52–500 on 400 amp and 65–625 on 500 amp) enable them to be used on practically all industrial applications ranging from light-duty, low-current to heavy-duty, high-current welding jobs.

Additional features include an automatic hot start which provides an extra surge of current for easier starting, arc stabilizing capacitors, power-factor capacitors, primary switch and an idlematic control and louvre canopy for adaptation to outdoor applications.

Descriptive bulleting GEC-1259 gives specifications and features on the G-E development.

Iron Powder Electrode

Introduction of a perfected iron powder electrode that is virtually self-cleaning has been announced by the Welding Products Division of the A. O. Smith Corp., Milwaukee, Wis.

Named the SW-44, the new electrode will enable the industrial user to deposit far more E-6012 type weld metal than with the conventional electrode, as well as almost eliminating the job of cleaning. There's good washup with no undercut, and bead appearance is flawlessly smooth with fine ripples. Re-striking is easy, even with a completely cooled rod, according to the manufacturer.

Further information on the SW-44 and other iron-powder electrodes can be obtained from A. O. Smith distributors.

Hobart "SLP" Welder

A new a-c type welder, the Hobart "SLP," is being announced by Hobart Brothers Co., Troy, Ohio. This machine is available in 300-, 400- and 500-amp sizes. It is a heavy-duty welder for a wide range of a-c welding production jobs.

The transformer is of the shell type design, using "Saturable Leakage Path" control for adjustment of welding current. An extra secondary winding supplies current to the control rectifier, which is of standard selenium type—40 v, d-c for safe, low voltage remote control. No high voltage appears across any control coils, eliminating possibility of insulation breakdown or high voltage appearing under the rheostat handle. The control is sensitive to a wide range of welding conditions and highly responsible to changing arc conditions so that light and heavy metals can be welded with the same machine.

The machine is designed for heavy-duty production welding—frame, core and case are welded steel. It has no moving parts: core and coils are anchored in place for quiet and trouble-free operation. It is capacitor equipped for power factor correction. Extreme high efficiency of the unit is due to "shell" transformer design and use of special steel in the cores, which keeps the no load losses to a minimum and increases full load efficiency. Three-wheeled portable mounting is optional equipment.

For complete information write to Hobart Brothers Co., Troy, Ohio.

Light Welder

Long believed impossible to design, a small, hand-carried welder weighing only 59 lb and guaranteed to weld with a $^{5}/_{32}$-in. electrode while operating on any single-phase, 110-v current has been announced by Royal Arc Industries, Inc., Chillicothe, Ill. The welder, known as the Royal Arc 200A, is particularly advantageous in industrial maintenance and repairs, since it can be carried about from building to building—or even factory yard space—

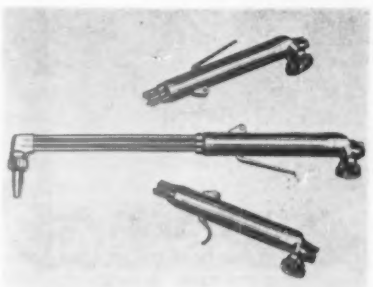
plugged into any 110- or 220-v outlet and perform all types of welding work.

The Royal Arc 200A has been tested by use in a wide range of applications for two years. The unit cuts, brazes, solders, preheats and can also be used to hard surface parts.

The Royal Arc 200A welds with electrodes from $^{5}/_{64}$ up to and including $^{5}/_{32}$ in. Any 110- or 220-v, 50/60 cycle, single-phase current is satisfactory, whether supplied by power company or portable generator.

Lightweight Cutting Torch

An important new development in gas cutting torches is announced by Smith Welding Equipment Corp. This new torch line combines light weight with heavy cutting capacity. Hard-drawn, pure copper cutting tips are of the "slip-in" design and have protected replaceable seats. The 3-in-1 torch handle may be rotated to accommodate a cutting jet lever in either underhandle or overhandle position. A trigger may also be installed in the underhandle location. With this design it is possible to accommodate practically any operator requirements as to cutting jet lever location.



The new lightweight cutting torch is available in head angles of 75 and 90 deg. Two- and three-hose models for use in all popular makes of automatic cutting machines are also available.

The newly designed slip-in tips are seated in the torch head with hand pressure on the tip nut. No wrench is necessary. The flame characteristics have been vastly improved and the standard cutting speeds of the six-preheat flame series are in the commonly accepted "high-speed" range. These same charac-

teristics are built into special cutting tips designed for gouging, rivet burning, scarfing, etc.

Full information may be obtained by writing for Bulletin 250. Address Smith Welding Equipment Corp., Department P854, 2633 Fourth St. S. E., Minneapolis 14, Minn.

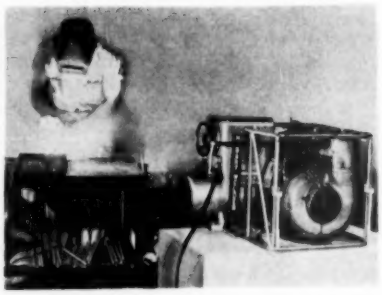
New Turbine-Type Arc Welder

Here is a glimpse of the first arc welder to use a gas turbine as a prime mover. It is a development of Hobart Brothers scientific research division at Troy, Ohio.

Pictured here is a 250-amp capacity d-c welding generator that was successfully operated and tested under actual working conditions. It is not a commercial venture, but rather a development with the idea of keeping abreast of the times in a fast-changing world of engineering and equipment.

The company in building this special unit, is primarily interested in knowing more about the gas turbine, and the possibilities it might hold for use in building more compact, lighter-weight equipment. Greatly reduced weight and size are decided advantages of using the gas turbine that could well be the power of tomorrow.

The company has built many jet engine starters for the air force and manufacturers, using the conventional internal



combustion engine. The turbine type of power may be the answer to lighter airborne equipment that could be easily transported in smaller aircraft than the present cargo plane.

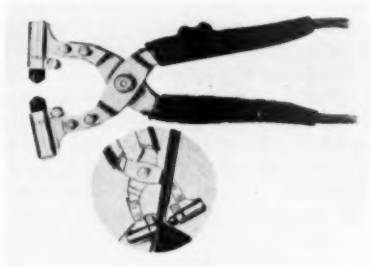
Hobart Brothers, manufacturers of all types of electric arc welders and electrodes, have just recently announced a new line of automatic and semiautomatic welding equipment, a-c transformers and rectifier welders.

Spot Welding and Soldering Pliers

A perfected spot welding and soldering pliers that works off any automobile storage battery of 6-12 v is now being marketed by the Tip Top Electric Products Co., 1440 Broadway, New York 18, N. Y.

Called Weldall, the pliers fill a long-felt need by repair shops, and light industry for an inexpensive spot welder that can be used anywhere—indoors and outdoors. No electrical outlet is needed.

The Weldall is an extremely versatile tool. It will spot weld and hard or soft solder steel, stainless steel, copper, aluminum, iron, wire, heavy brass, bronze, gold, silver and all other precious metals to



themselves and each other. Metal toys, furniture, appliances, pots, pans, wire fences, tanks, electrical parts, automobile connections, tools, power equipment, machinery, etc., can be repaired with Weldall.

The work to be welded or soldered is merely inserted between the carbon electrodes which form the jaws of the pliers. Current is turned on by means of a switch set in one of the handles. Two thousand degrees of instantaneous heat is generated in the carbon electrodes to form the union of metals. No welding glasses are required. The carbon electrodes are replaceable.

If a permanent indoor welding installation is desired, or if the user does not wish to employ his car storage battery, the Weldall can be worked off 110 v, 60 cycles with a transformer giving an output of 6 to 24 v at 60 to 150 amp.

Portable Welding Unit

Harvey Aluminum of Torrance, Calif., working in conjunction with Burdett Oxygen Co., has developed a compact portable welding unit for light gas welding. Designed originally for Harvey Aluminum's field engineers, the Burdett welding assembly can be used for all commercial aluminum alloys.

The basic unit consists of a full, 80-cu ft oxygen cylinder (weight 73 lb); a full B acetylene cylinder (weight 35 lb); and the accessory group consisting of torch and tips, regulators, twin hose, goggles wrench and file lighter (combined weight 17 lb). Additional equipment, such as cutting attachments, portable cylinder carts, larger cylinders, etc., are optional.

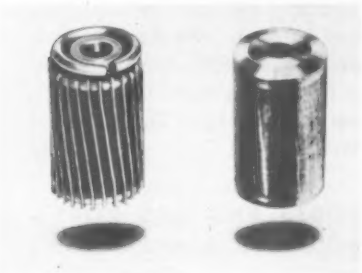
The portable welding unit is recommended especially for building contractors,

structural workers, refrigerator servicemen, electrical contractors, sheet metal installation, field welding, etc.

Complete information on the portable welding assembly can be obtained by writing to Burdett Oxygen Company of California, 2014 Belgrave Ave., Huntington Park, Calif.

Midget Rotors Induction Brazed

In one of the more complex brazing operations on record, Servomechanisms, Inc., producers of Servo Motors and other servomechanisms, induction braze midget rotors, integral parts of a Servo Motor with preplaced Phoson rings a product of United Wire & Supply Corp., Providence, R. I. The rotor brazed in this manner is



called upon to rotate at 400 revolutions per second at temperatures ranging from −150° F to over +200° F. The brazed joints in this bonding must have high conductivity and no flux can be used in joining. They must also be free of any tendencies toward corrosion or crystallization and must have exceptionally high strength.

A-C Arc Welder

A new a-c arc welder, featuring the first use of nonconductive Fibre-Glass in a welding cabinet is being introduced by Marquette Manufacturing Co., Inc., Minneapolis.

With this unique welding construction, "Perma-Shield," it is claimed that losses

from "eddy currents" and hysteresis are completely solved. According to the manufacturer, this new Fibre-Glass welder cabinet gives the advantages of a nonconductive, tough, lightweight material that is noiseless, weatherproof, corrosion-resistant, rustproof, and permanently colored.

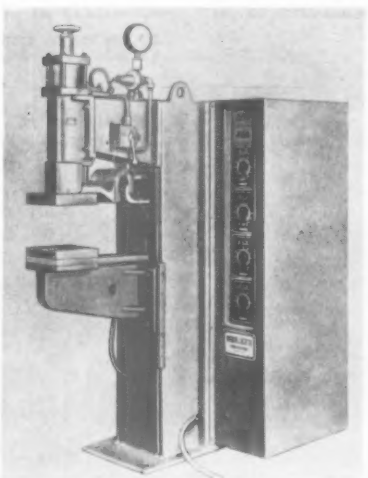
Featuring the "instant arc" for fast, easy arc striking, the welder produces a stable, surge-free welding arc easily handled even by nonprofessional welders.

For more information, write Marquette Manufacturing Co., Inc., 307 E. Hennepin Ave., Minneapolis 14, Minn.

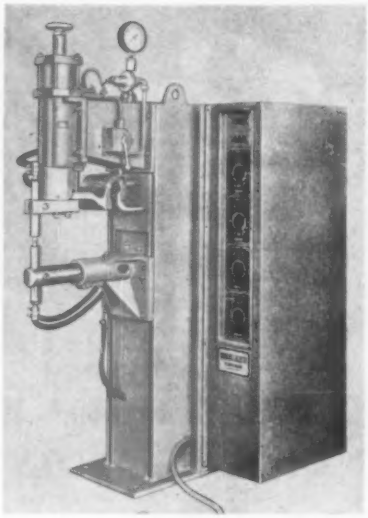
Projection and Spot Welders

Sciaky Bros., Inc., 4915 W. 67th St., Chicago, Ill., announce the new design of a standard, air-operated, press-type, low-impedance, single-phase FP1 projection and SP1 spot welders.

Both the SP1 and FP1 machines are one



Projection Welder



Spot Welder

of a series of single-phase spot and projection welders available in 30, 50 and 75 kva at 50% duty cycle. The FP1 can be supplied from 12- to 30-in. throat depth, while the SP1 can be supplied from 18- to 42-in. throat depth. The SP1 has an electrode force of 1000 to 1575 lb maximum, dependent on throat depth.

The Sciaky SP1 will make high-quality welds on large production runs on a wide range of gages of clean mild steel and stainless steel. By varying weld time, weld current and electrode force, even heavier gages can be joined at slower speeds.

For complete specifications on the SP1 and FP1 Sciaky welders, write for Bulletin 324-2.

Monel Electrode

A new Monel electrode, offering a fast rate of deposit, uniform bead characteristic and easy slag removal, with an exceptionally smooth arc, has been placed on the market by Pacific Welding Alloys Mfg. Co., Los Angeles, Calif.

Trade-named "Pacific Monel," the new electrode is ideal for metallic arc welding of Monel to Monel, Monel to steel and nickel to steel.

Pacific Monel is an all-position electrode and is manufactured with a special uniform flux coating for application by a-c or d-c equipment.

The new line of Pacific Monel electrodes is available in full range of sizes, and is packaged in hermetically sealed containers to prevent moisture pick-up and rod scuffing.

Descriptive literature, along with free samples, is available upon request to Pacific Welding Alloys Mfg. Co., 310 N. Avenue 21, Los Angeles 31, Calif.

RACO gives you UNINTERRUPTED

COSTS NO MORE ... SOMETIMES LESS

MILD STEEL • MEDIUM CARBON • LOW ALLOY
ALUMINUM • BRONZE • SPECIAL PURPOSE ALLOYS
CHROME-MOLY • STAINLESS • NICKEL • TITANIUM

THREADWOUND COILS
WIRE ON SPOOLS • CATCHWEIGHT COILS

STRAIGHT LINE FEED OFF—Threadwound coils assure straight line feed off. Final reduction to finished size is made on rewinding machines. All butt welds are pulled through finishing die eliminating bumps, kinks and cross overs. All coils have a cast placed in them with a four-inch maximum expansion . . . coils stay in place when tie bands are cut, avoiding looping over sides of pay off reels.

STEEL BANDS STIFFEN COILS—Threadwound coils are now being wound on corrugated steel bands for added stiffness.

UNIFORM WEIGHT—Threadwound coils are of uniform weight, automatically controlled by our rewinding machines.

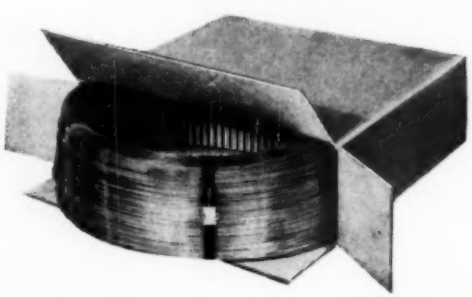
PALLETIZED CARDBOARD CARTONS—Threadwound coils are packaged in cardboard containers, then steel strapped on pallets manufactured in our own plant.

ENDS OF COILS—Back end of each coil is securely anchored to corrugated steel band . . . starting end is marked with colored tag and left straight for feeding automatic machines. No cutting or straightening needed.

ALL COPPER-COATED WIRE IS ELECTRO-PLATED

MODERN MANUFACTURING FACILITIES—We have complete and modern continuous wire drawing equipment, descaling and acid cleaning for hot-rolled wire rod. Annealing and normalizing equipment. We have fourteen wire straightening and cutting machines, and can furnish straightened and cut lengths up to 36″, in sizes 1/32″ to 5/16″. Catchweight coils up to 400 lbs. Threadwound coils in all standard widths and I.D. . . . all standard size spools.

MANUAL ELECTRODES AND GAS RODS—Available in 50-pound cartons, shipped on pallets at no extra charge.

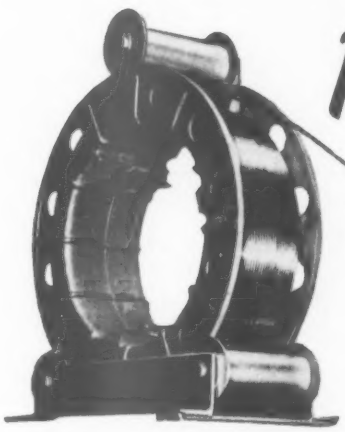


what it takes for

AUTOMATIC ARC-WELDING

RACO COMPOSITE TYPE "A"

Heavy Coated Open-Arc Electrode. Available in mild steel, low alloy-high tensile, low hydrogen, hard surfacing and stainless steel grades. Two other grades for open-arc welding—RACO TYPE "D" with white flux coating and RACO TYPE "M9" (knurled) with red flux.



plus

THE LATEST IN LABOR-SAVING EQUIPMENT

RACO FREE WHEELING REELS—SAVE DOWN TIME by using larger Raco coils. Ball-bearing trunnions enclose the coil so there is no overrun or loose strands and larger coils can be used. Changing coils is a matter of only a few seconds and no tools are required. Available in 12, 14, 25 and 36 inch sizes, for any weight from 50 to 200 pounds.

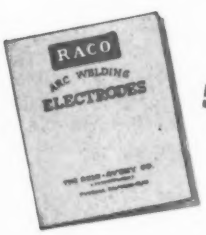
FOR SUBMERGED ARC-WELDING

The New RACO Model BW-2

Outstanding features are:

1. Adaptability to any job . . . nozzle can be positioned 360° in two planes.
2. Simplified control for either standard or constant potential welders, AC or DC.
3. Easy to install . . . just plug in head and hook up welding cables.
4. Available with various nozzles and attachments for submerged-arc, gas-shielded arc, or open-arc welding of all kinds. Also duplex nozzles and feed rolls for twin arc-welding.
5. Trouble-free operation . . . only three moving parts.

THE RACO TYPE 4R automatic head and controls are designed and furnished for use with RACO composite Type "A" wire.



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BALTIMORE 22, MARYLAND

SINCE 1919 PRODUCERS OF ARC-WELDING ELECTRODES AND WELDING RODS

SECTION NEWS AND EVENTS

as relayed to C. M. O'Leary

ASME Code

Albuquerque, N. Mex.—The *Albuquerque* Section held its first meeting of the season on September 24th at Leonard's Restaurant. A group of 40 heard J. L. Fry AWS, Chief Inspector, Denver Office of the Hartford Steam Boiler Inspection and Insurance Co., speak on the value of ASME Code and National Board Registration. A lively and lengthy discussion period followed Mr. Fry's presentation.

Hard Facing

Beaumont, Tex.—The *Sabine Division* of the Houston Section held its September meeting on September 23rd at the Edson Hotel Roof with an attendance of 73 at the dinner and meeting.

Speaker was R. P. Culbertson AWS, of the Haynes Stellite Co., Kokomo, Ind. Mr. Culbertson, who is considered one of the foremost authorities in the country on hard facing, gave a very interesting discussion of this subject. An excellent movie, "Story of Arc Welding," showing the techniques of all phases of arc welding was shown.

The Sabine Division has patterned their dinner meetings from the Houston Section's dinner meeting. That is, they start the evening off at 6:00 P.M. with a "Social" which is called a "Beer Keg." Members and visitors have about thirty minutes to mill around and meet new members, talk to visitors, talk shop with supply people and others from industry.

Newly Elected Officers

Chicago, Ill.—The *Chicago* Section announces the election of the following officers for the 1954–55 season:

Chairman—Amel R. Meyer, Graver Tank & Manufacturing Corp., East Chicago, Ind.

1st Vice-Chairman—Robert R. Lincoln, Air Reduction Sales Co., Chicago, Ill.

2nd Vice-Chairman—J. Bland, Standard Oil Company of Indiana, Whiting, Ind.

Secretary-Treasurer—L. C. Monroe, *Welders' Digest*, La Porte, Ind.

Tool and Die Welding

Chicago, Ill.—On Friday, September 24th, the opening meeting of the *Chicago* Section found a good turnout, numbering 159 members and guests. The new officers, with Amel R. Meyer presiding, got the new program off to a flying start.

Patrick S. Doyan, AWS, Sales Manager, Welding Equipment and Supply Co., Detroit, spoke on "Tool and Die Welding." In addition, he showed two sound slide films on this subject. The information was well received and the question-and-answer period was particularly long, reflecting the interest of those in attendance in this subject.

Prior to the technical talk, a premeeting movie called "The Saga of the Polywog," showing the trip of a family in an all-welded, shallow draft, houseboat type cruiser, powered by an outboard motor, which went down the Wisconsin, Mississippi, and up the Ohio rivers, was very well received.

A. G. "Bill" Craske of the Arcos Corp., heading the Hospitality Committee, had members of his committee wearing large buttons, which made them readily identified by the many new members who have joined the Chicago Section since the last meeting. The Hospitality Committee is actively seeking out these new members to make sure that they are made to feel welcome.

W. S. Boedecker, Chairman of the Golf Committee and representing the Bastian-Blessing Co., outlined the plans for the golf outing next June, and summarized the excellent results of the last outing.

O. T. Barnett, Chairman of the Special Activities Committee, affiliated with the Armour Research Foundation, gave a few introductory remarks on the Midwest Welding Conference which will be sponsored by the Chicago Section of the AMERICAN WELDING SOCIETY and the Armour Research Foundation jointly, on February 8th and 9th, at the Armour Research Foundation of the Illinois Institute of Technology in Chicago.

Dayton Program

Dayton, Ohio.—The following is a program of meetings to be held by the *Dayton* Section for the remainder of the season:

December 14th—"Resistance Welding and Resistance Welding Electrodes," E. F. Holt, Chief Engineer, Welding Division, P. R. Mallory & Co.

January 11th—Quiz Program with Panel—Resistance, Inert Arc, Coated Electrode Process, Gas Welding and Brazing

February 9th—"Welding Metallurgy," G. Linnert, Armco Steel Co. Joint dinner meeting with ASM

March 8th—"Contact Electrodes." Speaker to be announced

April 11th—"Automation," C. F. Hautau, Chief Engineer, Hautau Engineering Co. Joint meeting with ASTE, Sutmillers Restaurant

May 10th—Plant Visitation, Airtemp Division of Chrysler Corp.

June 14th—Annual Picnic

All meetings will be held at 8 P.M. in the Dayton Engineers Club, 112 E. Monument Ave., unless indicated otherwise.

Pipe Fabrication

Denver, Colo.—The regular monthly dinner meeting of the *Colorado* Section was held on September 14th in the Festival Room of the Oxford Hotel, Denver. Coffee speaker was Judge E. Johns of the Superior Court who spoke on the subject "Juvenile Deliquency."

H. Jackson AWS, of the Thompson Pipe & Steel Co., gave an excellent semi-technical presentation on the subject "Pipe Fabrication and Long Span Installation."

Newly Elected Officers

Detroit, Mich.—The *Detroit* Section announces the election of the following officers and Chairmen of Committees for the 1954–55 season:

Chairman—R. L. Clark, Unidex Cable Co., Ypsilanti, Mich.

1st Vice-Chairman—J. E. Rainey, Detroit Testing Lab., Detroit, Mich.

2nd Vice-Chairman—W. H. Smith, Weltronic Co., Detroit, Mich.

Secretary-Treasurer—J. R. Stitt, R. C. Mahon Co., Detroit, Mich.

Chairman, Membership Committee—Walter Kroy, Ford Motor Co., Dearborn, Mich.

Chairman, Program Committee—Harold Leininger, Composite Forgings, Inc., Detroit, Mich.

Technical Representative—Prof. L. E. Wagner, University of Michigan, Ann Arbor, Mich.

Brewery Methods

Fort Wayne, Ind.—The September 15th dinner meeting of the *Anthony Wayne* Section took place at the Centluire

Simplified Precision Welder Control

for easier maintenance and increased dependability

Class 8993 NEMA Type S2H Synchronous-Precision Controller available with 600 or 1200 frame contactor. Can be arranged either for side of machine or wall mounting.

DESIGN LEADERSHIP

APPLICATION: *for welding stainless steel, nonferrous metals, and other critical spot welding jobs requiring electronic synchronous-precision control. (Available with current wave shape control auxiliaries for aluminum or alloy metals.)*

DESIGN LEADERSHIP features

Easier Maintenance . . . greatly simplified circuits require fewer tubes and only one operating relay (for the air valve).

Increased Dependability . . . all components designed specifically for industrial welder service. Tube circuits are fail-safe and initiating contacts are in isolated 24 volt circuit which supplies power to the valve relay.

Consistent Accuracy . . . regulated DC voltage on tube control grids compensates for supply fluctuation or tube variations . . . assures timing accuracy.

Greater Accessibility . . . timer, heat control and firing panels designed for easy inspection and maintenance, with quick fasteners and plug connectors.

Write **for BULLETIN 8993**
or consult your nearby Square D Field Engineer

SQUARE D COMPANY

Brewery Co. John Meyers, Brewmaster, gave an excellent talk on the methods and processes of the brewery. Through the courtesy of the General Electric Co., a film entitled "Power for Freedom" was shown after the meeting.

Submerged Arc Welding

Grand Rapids, Mich.—The *Western Michigan* Section held their September meeting on Monday evening, September 27th, at the Varsity Grille in Grand Rapids, Mich.

The coffee talk was given by Donald Riser, Control Tower operator at the Kent County Airport. His talk was on modern control tower operations. It was very interesting due to the fact that Grand Rapids is becoming "air minded" and has 27 passenger flights into the port each day.

The Technical Session was addressed by A. F. Boucher [illegible], District Manager of the Lincoln Electric Co., in Detroit. His talk was on "Submerged Arc and Its Applications." Mr. Boucher's subject proved very interesting as evidenced by the large attendance and the several questions asked, with some of the members bringing parts to the meeting for Mr. Boucher to examine.

Social Meeting

Kansas City, Mo.—The *Kansas City* Section held their opening monthly dinner meeting in the Cypress Room of the Muehlebach Brewing Co. in Kansas City with 116 members and guests present.

This kick-off meeting was purely a social one so that all of the members could get reacquainted after the summer vacation period and it proved to be a highly successful one in view of the very large attendance.

Through the courtesy of the Muehlebach Brewing Co. a complete tour of the brewery was made before the dinner and everyone enjoyed this visit to the utmost.

Following a fine barbecue dinner in the Cypress, Chairman Phil Blake called to order a short meeting at which time he welcomed the members and guests. He then turned the meeting over to Tom Inchol, Program Chairman, who introduced Robert B. Coyner, Convention Manager of the Kansas City Chamber of Commerce. Mr. Coyner spoke on "What the National-Spring AMERICAN WELDING SOCIETY Convention Means to Kansas City." His talk was a very interesting one in that he gave facts and figures about Kansas City that many did not know before, such as hotel facilities, convention hall facilities, etc.

After such a big turn-out at the social meeting, the Kansas City Section expects real interest in the monthly meetings for the rest of the year.

New Section

Kingsport, Tenn.—The newly formed *Holston Valley* Section announces the election of the following officers:

Chairman—Q. E. Charlesworth, Bristol Steel & Iron Works, Bristol, Va.
1st Vice-Chairman—Scott Pyle, Kingsport, Tenn.
Secretary-Treasurer—R. T. Rogers, Oxygen Service Co., Kingsport, Tenn.

New Officers

Hollis, Long Island.—The *Long Island* Section announces the election of the following officers:

Chairman—E. W. Moles, Fairchild Engine & Airplane Corp., Farmingdale, L. I., N. Y.
Secretary-Treasurer—A. Strickland, Hollis, L. I., N. Y.
Chairman, Membership Committee—J. Jungbluth, Wantagh, L. I., N. Y.
Chairman, Program Committee—E. B. Morris, Grumman Aircraft Corp., Bethpage, L. I., N. Y.
Technical Representative—T. F. Imholz, Republic Aviation Corp., Farmingdale, L. I., N. Y.

Shielded and Submerged Processes

Hollis, Long Island.—The first regular dinner meeting of the *Long Island* Section for the 1954–55 season was held on September 16th at Anselmi's Restaurant, Bethpage, L. I.

James R. Craig, Eastern Region Engineer, of the Linde Air Products Co., was guest speaker. Mr. Craig's excellent and informative talk covered the topic "Shielded and Submerged Arc Welding Processes with Specific Applications in Metal Fabrication." The selection of proper process for a particular application, analyses of cost comparison and application data were stressed by Mr. Craig. The general discussion during the question-and-answer period following the talk testified to the interest by the 87 members and guests attending.

Preceding the talk, a film "Braze Welding" provided by the Linde Air Products Co. as a "coffee" item was of definite interest to those present.

East Texas Elections

Longview, Tex.—The following officers and Chairmen of Committees have been elected by the *East Texas* Section for the 1954–55 season:

Chairman—John B. Du Bose
1st Vice-Chairman—W. S. Coggins
2nd Vice-Chairman—I. M. Thompson
Secretary—Paul Broadstone
Treasurer—R. C. Wood
Chairman, Membership Committee—I. M. Thompson
Chairman, Program Committee—W. S. Coggins
Technical Representative—Paul Broadstone

Hardenable Electrodes

Los Angeles, Calif.—On September 2nd, a meeting of the Aircraft Counsel of the *Los Angeles* Section was held and the speaker for the evening was Leo West, Welding Engineer of Douglas Aircraft Co., Long Beach.

The subject of Mr. West's talk was: "Is Your Electrode Hardenable?"

Mr. West is one of the first welders employed by the Douglas Aircraft Co. and

UNITED STATES TESTING COMPANY, INC.

INSPECTION and TESTING of Welding and Weldments
QUALIFICATION of Procedures and Operators

Main Laboratories **Hoboken, N. J.**

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Effective March 1, 1953	1 time	3 times	6 times	12 times
Full Page	$255.00	$235.00	$220.00	$200.00
Two-thirds Page	180.00	165.00	155.00	145.00
Half Page	140.00	130.00	120.00	110.00
One-third Page	110.00	100.00	95.00	90.00
Quarter Page	85.00	80.00	75.00	70.00
One-sixth Page	65.00	60.00	55.00	48.00
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Special Preferred Position: (Full Page)	280.00	260.00	245.00	225.00
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2nd Cover				$250.00
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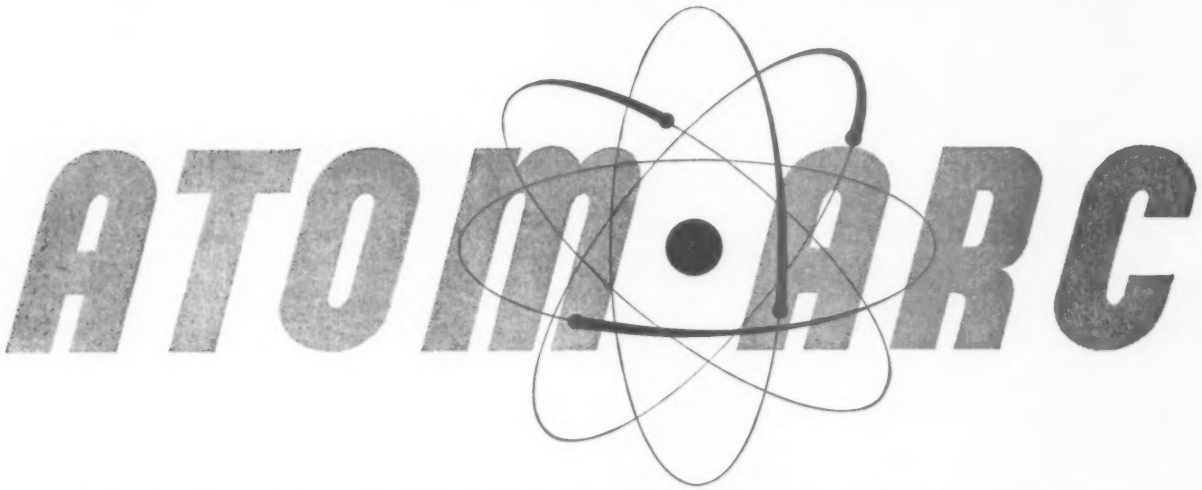
An Entirely New Concept of Low Hydrogen Welding



Combining the best in welding arc characteristics with the highest physical properties known to steel producers today . . . *ATOM* ● *ARC* electrodes meet the stern requirements of the Atomic Age.

Compare Atom • Arc with Ordinary Low Hydrogen Electrodes

Faster . . . For the first time in the industry, *ATOM* ● *ARC* utilizes iron powder in the electrode coating of a low hydrogen electrode—making possible the use of higher welding currents with either A.C. or D.C.

Greater Deposition Efficiency . . . Amazing deposition efficiencies are made possible because iron powder contained in the electrode coating serves as an additional source of weld metal.

No Moisture Pick Up . . . *ATOM* ● *ARC* is packed in 50-lb. hermetically sealed metal containers. Electrodes will effectively resist detrimental moisture pick up for ten days after opening container.

Weld Appearance . . . The full covering, easy to remove slag helps to form a smooth, uniform weld bead—free of undercut.

Ease of Welding . . . *ATOM* ● *ARC* reduces welder fatigue because the heavy electrode coating permits contact with the work in all positions.

Highest Physical Properties . . . *ATOM* ● *ARC* is manufactured in six A.W.S. low hydrogen grades and will meet all specifications for these types of electrodes.

At the *METAL SHOW* . . . See ATOM • ARC demonstrated at the Alloy Rods Company Booth #1359 at the Metal Congress, Chicago, November 1 to 6. Call your Alloy Rods Company distributor or write to nearest plant for further information.

ALLOY RODS COMPANY

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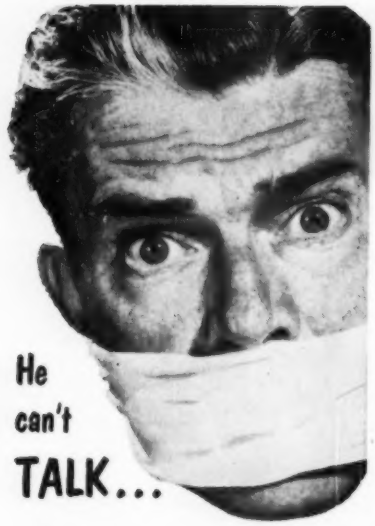

(So we'll say it for him.) The ingenious new SMITH'S "Flo-Trol" design eliminates back-fire and prevents burned-out valve seats in Smith's oxy-acetylene cutting torches. It's fool-proof. You can't go wrong.

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SMITH WELDING EQUIPMENT CORPORATION

Dept. WJ-111 2633 S. E. 4th St. Minneapolis, Minn.

has over 25 years of experience, both in welding and as an engineer. His paper was very well prepared and covered the welding of all the aircraft steels and there was a lively discussion as to the new military specifications and the Qualified Products Lists being set up by the Air Force on the welding of these aircraft steels.

Program of Meetings

Milwaukee, Wis.—The regular meeting dates of the *Milwaukee* Section for the 1954–55 season are as follows:

November 19th—"Arc Air Process and Uses in Industry," E. J. Lehmkuehl
December 17th—"Resistance Welding," A. F. Wood
January 21st—"Jigs and Fixtures for Hand Welding," H. G. Frammer
February 25th—"Welding Metallurgy," C. L. Altenburger
March 25th—"Inert Gas, Metal Arc Consumable Electrode Welding," J. E. Fitzwater
April 22nd—"High-Speed Submerged Arc Welding of Mild Steel with Multiple Arcs," W. H. Schuster
May 21st—May Party

Arc Welding Electrodes

Marion, Ohio.—The first regular meeting of the newly formed *North Central Ohio* Section was held Friday evening, September 10th, at Marion, Ohio. This was a dinner meeting held at the Hotel Harding and was attended by 66 members and guests.

The program for the evening consisted of a football film supplied by the Wilson Sporting Goods Co. and a technical speaker. A. Clemens, Jr., [AWS], of the Arcrods Corp., Sparrows Point, Md., talked on the subject, "Some of the Latest Developments in Arc Welding Electrodes." Mr. Clemens' talk was concerned primarily with the new iron powdered coating type electrodes, their applications and limitations.

John Blankenbuehler, District Director, attended the meeting and spoke briefly to the new Section. The other officers and committeemen were introduced. James Dipert was appointed 2nd Vice-Chairman of the Section to replace Terry Long who had transferred out of the area.

Meeting Locations

Marion, Ohio.—The *North Central Ohio* Section meetings will be held on the first Friday of each month at the locations listed below:

December—Marion
January—Marion
February—Marion
March—Plant Visitation, Baldwin-Lima-Hamilton Corp., Lima, Ohio
April—Mansfield
May—Bucyrus
June—Stag-Golf, Mar-O-Del, Marion, Ohio

Simplified Metallurgy

Newark, N. J.—On Tuesday, September 21st, the *New Jersey* Section had dinner at the Essex House in Newark, N. J. The dinner was followed with a "Coffee Time" speaker, Fred E. Haley. Mr. Haley is the District Parole Supervisor for the New Jersey Bureau of Parole. He spoke on the functions of the Parole Board. His interesting speech was well received by everyone.

Chairman Ken Koopman opened the meeting at 8:15 P.M. Approximately 100 members and guests were present. Mr. Koopman introduced the speaker, Anton Schaeffler [AWS], who is a metallurgist in welding research. Mr. Schaeffler received his undergraduate training at Marquette University, and a masters degree at the University of Wisconsin. He worked as a cooperative student at Harnischfeger Corp. Following graduation in 1942, Mr. Schaeffler spent three years at A. O. Smith Corp. From 1945 to 1950, he had charge of the metallurgical section at Arcos Corp. in Philadelphia, where he specialized in the welding of stainless and dissimilar metals. He is now working on welding problems in the Research Laboratories of Allis-Chalmers Manufacturing Co. in Milwaukee.

Mr. Schaeffler's well-presented speech covered the major factors welders must consider when welding mild steel, low-alloy steel, stainless steel and dissimilar steels.

After the meeting, the usual refreshments and social hour were enjoyed.

On Saturday, September 25th, the Annual Outing was held at the Old Cider Mill in Union, N. J. Approximately 550 people attended. Everyone participated in the various games and events which were scheduled. Chairman of the Outing was Robert Thornton, assisted by Steve Yatsko and officers of the New Jersey Section.

Early Program News

Newark, N. J.—The tentative list of subjects for the monthly meetings of the *New Jersey* Section is as follows:

November—Shielded-Inert-Gas Metal-Arc Welding
December—Welding Equipment—Jigs, Fixture, Positioners, Applications
January—Joint meeting with ASME (atomic subjects—"Isotopes in Industry")
February—Weldability
March—Welding Standards and Codes
April—Practical Fabrication
May—Special Year-End Meeting
September—Picnic—Saturday, September 25th, at the Old Cider Mill, 186 Vauxhall Road, Union, N. J.

The meetings are held on the third Tuesday of each month except for December 14, 1954.

Each of the meetings will include the following functions:

5:30—Executive meeting
6:30—AWS Dinner $2.50. Open to all without reservations. However, should a member plan to bring a group or a number of guests to dinner, delays will be avoided by contacting Howard Hughey, Air Reduction Co., Clermont Terrace, Union, N. J. Phone: Elizabeth 4-1212
7:45—Coffee speaker or film; alternate every meeting (nontechnical)
8:00—Main speaker
8:45–9:00—Question period
9:15—Refreshments

Welding Metallurgy

New York, N. Y.—"Welding Metallurgy" was the subject of a paper presented to the *New York* Section on Sep-

Now NCG® creates a truly all-purpose all-position electrode for welding mild steel

THE NEW NCG SUREWELD® "CE" ELECTRODE

Here are reports from the field:

"The best all-around AC rod I've ever used."

"We are getting very good results with CE. And we like the way the slag practically drops off when cool."

"Beads have exceptionally smooth surface."

"In our book it surpasses any other E-6013 on the market."

"We think it's superior to all other 6013 electrodes in running characteristics."

"We find we can use it on more jobs than any other electrode we've tried."

NCG's new "CE" Electrode is designed to perform with maximum efficiency over a wider range of applications. It is usable in all positions with AC or DC straight or reverse polarity. Operators like this new rod because it is easy to strike and hold an arc, even with AC at low welding currents and low open-circuit voltages. There is little spatter; the arc is soft; action is quieter, with spray type metal transfer. Beads are smoother, with a fine, even ripple—free from pock-marks and slag inclusions. Slag deposits brush off easily. "CE" deposits with remarkable efficiency in any position. *Sureweld* "CE" conforms in all respects to E-6013 specifications. Your NCG dealer or nearest NCG office will be glad to demonstrate this new rod and let you try it for yourself. Contact him today.

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EVERYTHING FOR WELDING

tember 14th. Dr. W. D. Doty [AWS], U. S. Steel Corp. Welding Research Laboratory was the speaker. Dr. Doty outlined the metallurgical changes that take place in a weldment and the effect of the various welding processes on such changes as a function of the energy input associated with the processes discussed. Excellent slides were used to illustrate the discussion as well as to show many welding defects commonly encountered in welding. Dr. Doty also discussed the methods of eliminating or remedying these defects.

The meeting was held at Schwartz's Restaurant, 54 Broad St., New York City, where dinner was served prior to the technical session. Mr. Herman C. Phelps served as Technical Chairman.

Newly Elected Officers

Oklahoma City, Okla.—The *Oklahoma City* Section announces the election of the following officers and Committee Chairmen for the 1954–55 season:

Chairman—James N. Thompson
1st Vice-Chairman—Levi Nelson
Secretary—Robert A. Bernier
Treasurer—Lon Erickson
Chairman, Program Committee—C. L. Conner
Technical Representative—Paul Wedel

Installation of New Officers

Olean, N. Y.—The September meeting of the *Olean-Bradford* Section, held September 27th, at the Castle Restaurant, was well attended. The following officers were installed:

Chairman—Anthony Lorenzini, Daystrom Furniture, Olean, N. Y.
1st Vice-Chairman—Guy Whipple, Kendall Refining Co., Bradford, Pa.
2nd Vice-Chairman—Charles Smith, Pierce Engineering Co., Olean, N. Y.
Secretary—Andrew Kasper, Daystrom Furniture, Olean, N. Y.
Treasurer—Sid L. Worth, Catt. Co. Highways, Little Valley, N. Y.

A short talk was given by Charlie Thomas [AWS], Dresser Industries, Bradford, Pa., on the advantages of belonging to the AMERICAN WELDING SOCIETY.

Members were present from Olean, Bradford, Wellsville and Little Valley.

Structural Welding Developments

Phoenix, Ariz.—The September 15th dinner meeting of the *Arizona* Section was held at the Silver Spur. C. N. "Chuck" Corbit [AWS], of the American Institute of Steel Construction, covered the subject "Developments in Structural Welding."

Schedule of Meetings

Pittsburgh, Pa.—A schedule of meetings, subjects and speakers of the *Pittsburgh* Section for the remainder of the 1954–55 season is as follows:

November 17th—"Heavy Plate Welding in Pressure Vessel Fabrication," N. C. Jessen, Welding Engineer, Works Control Laboratory, Babcox & Wilcox Co., Boiler Division, Barberton, Ohio
January 19th—"Magnetic Flux Welding," Howard S. Avery, Research Metallurgist, American Brake Shoe Co., Mahwah, N. J.
February 16th—"Inert-Gas Metal-Arc Welding of Carbon Steel," James R. Craig, Region Engineer, Linde Air Products Co., a division of Union Carbide & Carbon Corp., N. Y.
March 16th—"Quenched and Tempered Plate Steels for Pressure Vessel Construction," Leon C. Bibber, Chief Research Engineer, Welding Research and Technology Division, United States Steel Corp., Pittsburgh, Pa.
April 29th—Eighteenth Annual Tri-State Ladies' Night and Dinner, Georgian Room, Webster Hall Hotel.

Plant Visitation

Pittsburgh, Pa.—The September meeting of the *Pittsburgh* Section, held on the 24th, was in the nature of a plant visit to the Butler Division of the Armco Steel Corp., Butler, Pa.

Past-Chairman T. W. (Ted) Morgan, Welding Engineer, gave a short briefing on what the members would be shown on the trip through the plant. Members witnessed complete plant operations—from melting through pouring, rolling, polishing, inspection and shipping, steel sheets. Sixty-eight members of the Section took part in this conducted tour through the entire plant.

Welding Diesel Locomotives

Portland, Ore.—The first meeting of the season of the *Portland* Section was held on September 14th at the Heathman Hotel. An almost record attendance of 49 was greatly enthused over the program of the evening which consisted of a talk accompanied with slides by Harry S. Swan [AWS], Superintendent at Willamette Iron & Steel Co., Portland. His talk on "Welding the Modern Diesel Locomotive" was very well presented and held the interest of all.

A short business meeting was held preceding the dinner meeting at which it was announced that, due to a few changes in the Section officers, the following group are now officially appointed:

Chairman—B. N. Wood, Metallurgical Engineers, Inc.
1st Vice-Chairman—George A. Conner, Williamette Iron & Steel Co.
Secretary—F. M. Parker, 2720 N. W. Yeon Ave., Portland 7.
Treasurer—Earl Babbitt, Beall Pipe & Tank Co.

Gas Turbine

Saginaw, Mich.—The gas turbine's future for powering automobiles, trucks or buses depends on developments still in their early stages, a member of General Motors Research Laboratories told the meeting of the *Saginaw Valley* Section held on Thursday evening, September 9th, at the High Life Inn, Saginaw. The meeting was attended by 95 members and guests.

The speaker, William A. Turunen, Head of the Gas Turbines Department, said such problems as fuel economy, delayed acceleration and lack of engine braking—together with the need for costly high temperature alloys—must be solved if the turbine replaces piston engines in the nation's motor cars and other vehicles.

These are the most significant problems, some anticipated and some unforeseen, that have appeared in two General Motors test vehicles, the XP-21 Firebird, first turbine automobile in the United States, and the GM Turbocruiser, first turbine bus in the world, Mr. Turunen reported.

Meanwhile, he explained, the turbine has better weight-to-power ratio than either automotive or Diesel engines. Its ability to produce maximum torque at stall and operate on relatively low-grade fuels are additional advantages.

Inherently a simple mechanism with no reciprocating parts, it is almost completely free to vibration and applies power so smoothly that maximum traction can be maintained during acceleration, Mr. Turunen said.

He explained that such problems as high operating temperatures, air consumption, large quantities of exhaust gas, engine controls, noise levels, durability, maintenance and starting proved less bothersome than engineers had anticipated before either the Firebird or Turbocruiser was road tested.

Schedule of Meetings

Schenectady, N. Y.—The *Northern New York* Section announces a schedule of meetings, subjects and speakers for the remainder of the 1954–55 season as follows.

Nov. 18, 1954—"Filler Arc Welding," R. W. Tuthill, General Electric Co.
Technical Activities Display—Fundamentals of Welding, Rensselaer Polytechnic Institute, Troy, N. Y.
Dec. 9, 1954—"Tooling for Welding," C. C. Peck, Cecil C. Peck Co.
Technical Activities Display—Industrial Applications, Designing for Welding, Panetta's Restaurant, Menands, N. Y.
Feb. 3, 1955—"Nondestructive Testing of Weldments," C. D. Moriarity, General Electric Co.

Technical Activities Display—Training Inspection and Control, Panetta's Restaurant, Menands, N. Y.

Mar. 3, 1955—"Welding, Soldering, Brazing of Aluminum," C. Bruno, Reynolds Metals Co.

Technical Activities Display—W. R. C. Bulletins, Standards and Recommended Practices and Procedures for Spot Welding Aluminum Alloys. Panetta's Restaurant, Menands, N. Y.

Apr. 14, 1955—"Position All Three: The Man, The Weldment, The Automatic Head," A. K. Pandjiris, Pandjiris Weldment.

Technical Activities Display—Review of Categories and Ordering Procedures, Pittsfield, Mass.

May 19, 1955—Annual Meeting. Ladies' Night

Brewhouse Equipment

Seattle, Wash.—The September dinner meeting of the *Puget Sound* Section was held on September 9th at the Engineers Club, Arctic Building.

The after-dinner feature was the presentation of pins to all Past Chairmen of the Puget Sound Section.

An extemporaneous semitechnical talk on the "Fabrication and Welding of Brewhouse Equipment" was given by George Dexter, Vice-President of the Puget Sound Sheet Metal Works.

A field trip to the Rainier Brewery was held prior to the meeting.

Weld Design

South Bend, Ind.—The *Michiana* Section held its first meeting of the season on September 16th at the Red Barn Restaurant just north of Mishowaka, Ind.

After a Swiss steak dinner, a talk was given by Richard B. Loynd of the Lincoln Electric Co., Chicago District, on the subject "Weld Design and Redesign."

Mr. Loynd's able presentation was accompanied by illustrations and motion pictures on the same subject.

The approach to every design problem which should be divided into a number of interrelated factors such as function, intended durability, appearance, allowable cost, etc., was of major interest to most of those present.

Design of Welded Joints

Syracuse, N. Y.—The first meeting of the *Syracuse* Section's 1954–55 season was held at the Onondaga Hotel in Syracuse, N. Y., on September 8th. This meeting was highlighted by a talk given by A. N. Kugler (M'42), Chief Welding Engineer of the Air Reduction Sales Co.

Mr. Kugler, who spoke on "The Design of Welded Joints," presented a very interesting discussion on good and bad joint design from the standpoints of quality, efficiency, and economy. Step by step Mr. Kugler graphically illustrated his talk by blackboard drawings. The entire discussion was extremely interesting to engineers and operators alike, and was followed by a spirited question-and-answer period.

The regular monthly dinner, held in the meeting rooms prior to the technical session, was well attended, and the coffee speaker, Richard Keller of the Syracuse China Co., gave an extremely interesting talk on "The Manufacture of Pottery."

Plant Visit

Warren, Ohio.—On Thursday, September 16th, members of the *Mahoning Valley* Section visited the plant of the Copperweld Steel Co. in Warren, Ohio, under the direction of F. M. Hodd, Personnel Director, assisted by other members of the organization.

The Copperweld Steel Co. personnel expended a good deal of effort and time in planning this interesting plant visit which made it possible for the Section's new season to get off to a good start.

LIST OF NEW MEMBERS

Effective August 31, 1954

MEMBERSHIP CLASSIFICATION

A—Sustaining Member
B—Member
C—Associate Member
D—Student Member
E—Honorary Member
F—Life Member

ARIZONA

Steward, Roswell J. (C)
Worklan, John N. (C)
Law, William H. (C)

ATLANTA

Wolfe, Arthur D., Jr. (B)

BOSTON

Deacon, Frederick D. (C)
Haggett, Paul E. (C)
Jarvis, John H. (C)
Kropp, Louis F. (B)
Luongo, Joseph C. (C)
Malta, William A. (C)
Murray, Ralph (C)
Pevelli, Charles A. (C)
Rivoira, David D. (C)
White, Lawrence J. (B)

CHICAGO

Akin, L. P. (C)
Miller, Paul E. (C)
Olson, G. C. (C)
Roberts, H. J. (C)
Runkel, Wm. H. (B)
Tewell, Charles (B)

CLEVELAND

Bevi, Fred (C)
Cessna, F. M. (B)
Crittenden, John Carver (B)
Drechsler, Albert C. (B)
Green, Thomas A. (B)
Kunotz, Ewald E., Jr. (C)
Mendelson, Robert R. (C)
Miller, Loren C. (B)
Pabeschitz, Frank, Jr. (C)
Straquadine, Anthony (B)

COLORADO

Heimerich, G. Ralph (B)
Taylor, Kastler (B)

COLUMBUS

Weare, Norman E. (C)

DAYTON

Feinthal, B. K. (C)
Fels, Richard (C)
Weber, Robert G. (B)

DETROIT

Brown, Hugh M. (B)
Cenkner, John J. (B)
Curry, John A. (B)
Decker, Edward D. (B)
Hole, Lowell O. (B)
Mueller, John E. (B)
Ross, Victor D. (B)
Smith, Wellesley M. (C)

HARTFORD

Angelo, Michael (C)
Dettenborn, Lawrence J. (C)
Swider, Joseph C. (B)

HOUSTON

Barbin, LeRoy M. (C)
Briley, Jessie A. (C)
Muckelroy, R. V., Jr. (C)
Parker, Elwood L. (B)
Scales, Stanley R. (C)

INDIANA

Mann, Wayne B. (B)

KANSAS CITY

Bradbury, W. Edward (B)
Evans, Wayne (B)
Kirsch, Edgar W. (B)
Magers, Don (C)
Newton, William James (C)
Ragsdale, William (B)
Seat, Jack W. (B)
Stamm, Vincil R. (B)

LEHIGH VALLEY

Ruth, Richard E. (C)

LONG ISLAND
Brust, R. E. (C)
Thomas, John W. (C)

LOS ANGELES
Anderson, Arnold E. (B)
Booth, C. E. (C)
Lampert, Albert T. (C)
Maag, Donald B., Jr. (B)
Murphy, George P. (C)
Zowolkow, David Z. (C)

LOUISVILLE
Carson, David C., Sr. (B)
Sohl, Raymond J., Jr. (B)

MAHONING VALLEY
Bellay, George, Jr. (C)
Chos, Stephen (C)

MARYLAND
Barnhart, William R. (B)
Schnitker, Frederick F. J. (B)
Ugiansky, Joseph H. (C)

MILWAUKEE
Banks, James C. (B)
Laik, Theodore L. (C)
Nuber, Floyd J., Jr. (B)

NASHVILLE
Lester, G. H., Jr. (B)

NEW JERSEY
Chapman, David E. (B)
Collander, Joseph (B)
Dempsey, John J. (B)
Fichtmueller, F. (B)
Glass, J. R. (C)
McComb, Walter (B)
Pearson, Sylvester (B)
Sparks, William E. (B)
Thompson, Roy W. (B)
Wright, Jack (C)

NEW ORLEANS
Amedee, Laurence A. (C)
Armatis, Henry G. (B)
Bateman, William E. (C)
Blackwell, J. W. (B)
Cashen, John R. (B)
Delise, Anthony (C)
Du Frene, C. G. (C)
Dukes, Hadron Buddy (B)
Fothe, William A. (B)
Gaudet, Earl (C)
Gleaser, Francis J. (C)
Grashoff, Walter H. (B)
Mailhes, Edward A. (C)
Mercier, Joffre P. (B)
Moseley, F. E. (B)
Nail, Marvin J. (C)
Rebold, Johnnie E. (B)
Ritter, Skelton L. (C)
Roussel, Herbert J. (B)
Roussell, Leslie J. (B)
Sausse Betrand B. (C)
Somme, Ferdinand (B)
Tudury, Wilday (B)

NEW YORK
Boathe, Darrell T. (C)
Brosnan, Francis C. (B)
Craig, J. R. (C)
Jarl, Bert B. (C)
Martin, H. E. (B)
Mazzaferro, Joseph J. (C)
Morse, Ed. R. (B)
O'Brien, Frank J. (C)
Powers, James C., Jr. (B)
Schmitt, Philip (C)
Weisent, Harry O. (C)

NIAGARA FRONTIER
Luszcz, Walter C. (B)

NORTH CENTRAL OHIO
Crites, George Ullman (B)
Welling, John A. (B)

NORTHWEST
Harris, Edwin W. (C)
Hogen, Louis L. (B)
Russell, R. H. (C)

OKLAHOMA CITY
Berney, J. P. (C)

OLEAN-BRADFORD
Johnson, Richard D. (B)
Langley, Albert W. (B)
Pierce, Charles W. (B)

PASCAGOULA
Thompson, James T. (C)

PEORIA
Clark, Ralph E. (B)
Scott, Robert G. (B)

PHILADELPHIA
McKenzie, Alexander (C)
Powers, James J., Jr. (C)

PITTSBURGH
Hawkins, J. F. (C)
Motl, Byron C. (B)
McKee, Robert M. (B)

PUGET SOUND
Holt, Richard E. (C)
Janicki, Stanley, Jr. (C)
Nollmann, E. C. (C)

SAGINAW VALLEY
Bottke, Harvey D. (C)
Cole, Joe (B)
Tingler, William F. (B)

SAINT LOUIS
Blasiol, Victor John (C)
Hartley, Thomas M. (C)
Smith, Hugh T. (B)

SAN FRANCISCO
Cupit, A. P. (B)

TOLEDO
Welty, Kenneth R. (B)

WASHINGTON
Whitehead, Douglas Eugene (C)

YORK-CENTRAL PENNSYLVANIA
Little, Henry E. (B)

NOT IN SECTIONS
Bain, A. M. (B)
Cestero, Jose, Jr. (B)
Kenny, George W. (C)
Fujiie, Mango (B)
Weise, Dietrich Hans (B)

Members Reclassified

During the month of September

ARIZONA
Walker, Don (C to B)

CLEVELAND
Terbeck, George A. (C to B)

COLORADO
Garver, W. L. (C to B)

DALLAS
Bryan, Henry B. (C to B)

DAYTON
Bryan, Henry B. (C to B)

HOLSTON VALLEY
Charlesworth, Q. E. (C to B)

MAHONING VALLEY
Hinst, H. F. (C to B)

MARYLAND
Brainard, William E. (C to B)

NEW JERSEY
Helmbrecht, W. M. (C to B)
Oyler, Glenn W. (C to B)

NORTH CENTRAL OHIO
Ellison, Robert A. (C to B)

PITTSBURGH
Rutherford, John J. B. (C to B)

WESTERN MICHIGAN
Harvey, Douglas J. (C to B)

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abstracts of CURRENT WELDING PATENTS

prepared by V. L. Oldham

Printed copies of patents may be obtained for 25¢ from the Commissioner of Patents, Washington 25, D. C.

2,686,480—Welded Carside—Carl E. Johansson, Cleveland, Ohio, assignor to the Youngstown Steel Door Co., Cleveland, Ohio, a corporation of Ohio.

A welded side for railway cars is disclosed by this patent, which side includes a side plate, a plurality of pieces of side sill, a door and door posts. Various parts of the side of the car are welded together using special members provided thereon to aid in obtaining a sturdy welded construction.

2,688,065—Electric Welder—Nelson O. Clark, Winthrop, Mass.

This patent relates to an electric welder wherein a stationary electrode is provided having a terminus of an adjustable work support, and a mobile electrode movable relative to the stationary electrode is present and has a resilient blade extension thereon for flexible contact with the work. Means are provided for moving the mobile electrode toward the stationary electrode to effect engagement of the work support by the resilient extension on the mobile electrode. An electric power source is provided and has a circuit provided therefor including a capacitance, and an electric switch operative simultaneously with the mobile electrode to control the timing of the discharge of electrical energy from the capacitance through the electrode terminal during their engagement. Switch actuating means are present to move the switch from a normal circuit-opening position to its circuit-closing position and then

back to its normal circuit-opening position.

2,688,066—Hydraulic Flash Welder Control—Kingsley A. Doutt, Alpena, Mich.

This patent is on hydraulic apparatus for a flash welder having a normally fixed platen and a movable platen operated by a fluid-pressure motor. A hydraulic motor and a control valve therefor are provided whereas a secondary fluid-pressure-operated motor is provided and variable motion transmitting mechanism connect the second fluid-pressure-operated motor and one valve element of the control valve for the first motor. A third fluid-pressure-operated motor is present for moving the second valve element in the control valve of the first motor to initially by-pass hydraulic fluid to bring the movable platen to starting position in the welding cycle, after which the first element of the control valve will be moved to control the platen so that it will have a predetermined position-speed relationship during the flashing period of the welding cycle.

2,689,295—Welding Apparatus—George H. Goldner, Warren, Ohio, assignor to Federal Machine and Welder Co., Warren, Ohio, a corporation of Ohio.

Goldner's patent is on a movable electrode assembly for a low-inertia resistance-welding machine having quick weld follow-up characteristics. The assembly includes a piston and slide member positioned in a cylinder for movement in a direction toward the work to be welded and having a minimum of resistance to such movement whereby the end of the movable electrode will follow up the receding work surface as such surface is softened under the heat generated by the welding current.

2,689,323—Electronic Welding System—Hymen Diamond, Pittsburgh, Pa., and John R. Parsons, Kenmore, N. Y., assignors to Westinghouse Electric Corp., East Pittsburgh, Pa., a corporation of Pennsylvania.

This patent relates to special electronic controls for welding action and includes terminals for connection to a three-phase commercial power supply, a transformer having three primary windings, a plurality of ignitrons with each of the primary windings being connected across a pair of terminals for connection to a phase of the power supply in series with an ignitron. A mechanical reversing switch having forward and reverse positions is interposed between each primary winding and its respective supply terminals and means are present for rendering the ignitrons conductive in succession for a first time interval and at the end of the interval permitting the ignitrons to become nonconductive while the switch is in its forward position. Other means maintain the ignitrons conductive during the second time interval and means actuate the reversing switches during the second interval. Other means thereafter render the ignitrons conductive during the third time interval with the switch in the reverse position.

2,689,899—Welding Apparatus—Donald P. Faulk, Lexington, and Peter A. Best, Newton Highlands, Mass., assignors to Raytheon Manufacturing Co., Newton, Mass., a corporation of Delaware.

This patent relates to an electric welding head including a pair of intersecting arms pivoted intermediate their ends and each having an electrode mounted at one end thereof. Means for applying force to the other ends of the arms are present in the welding head. An electromagnetic means is provided and is operable upon the electrodes and responsive to the welding current for applying an additional force to the workpieces whose changes in magnitude are substantially in phase with changes in magnitude of the welding current when a workpiece is engaged by the movable ends of the arms.

2,690,492—Welding Nozzle Guide Arm for Spiral Pipe Machines—Ralph E. Asbury, Waynesville, and Dale Wiley, Middletown, Ohio, assignors to Armco Steel Corp., Middletown, Ohio, a corporation of Ohio.

This machine relates to welding spiral pipe in which strip material is fed between forming rolls at an angle and is formed into a helix and in which a welding nozzle holder is disposed substantially at the point where the edge of the first convolution meets the edge of the entering strip. The patent discloses a special mounting means for the nozzle holder so that it can pivot in accordance with lateral variations in the position of said entering strip edge.

Continued from page 1100

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Supplement to the Welding Journal, November 1954

Investigation of Factors Determining the Tensile Strength of Brazed Joints

Tensile strength of brazed joints properly made is dependent on base metal strength and its plastic constraint

by Nikolajs Bredzs

Abstract

For the fundamental investigation of the factors determining the tensile strength of brazed joints, a reproducible method of brazing of tensile test bars by means of induction heating has been developed. Using this method four tensile strength-joint thickness curves of low-carbon steel bars brazed with pure silver, pure copper, BAg8 and BAgMn filler metals have been obtained. These curves prove that the increase of tensile strength of brazed joints with decreasing thickness of the joint is due primarily to the influence of plastic constraint. With these experiments the hypothesis that such a strength increase is caused by intermetallic compounds or diffusion of the basic metal into filler metal is relegated to a secondary role. It has been proved also that a decrease of the tensile strength in extremely thin joints is due chiefly to the imperfections of such joints.

The comparative strength of pure silver and pure copper brazed butt joints in mild steel and drill rod has demonstrated the relationship between joint strength and base metal strength. Evidence is presented to show that the joint strength increases with increased strength of the base metal.

In a study of the imperfections of the brazed joints and the influence of these imperfections upon the tensile strength of the joints, two principal types of such imperfections have been investigated: (1) gas inclusions, and (2) imperfections at the base metal-filler metal interface.

A. INTRODUCTION

ONE of the principal objectives of the research of the fundamentals of brazing is the improvement of the strength of the brazed joints. All factors which determine and influence this strength, especially tensile, shear and fatigue strength, are important and therefore should be considered and analyzed. The strength of the brazed joint is chiefly determined by the nature of the metallic bond. As Case[1] states: "In any brazing operation, the nature of the bond between the base metal and filler alloy is a complex function of temperature, time and respective composition of the two metals." However, the strength of the brazed joint is not determined by these factors alone. Case quotes some investigations which show that the strength of brazed joints is strongly influenced by the film thickness of the filler metal also.

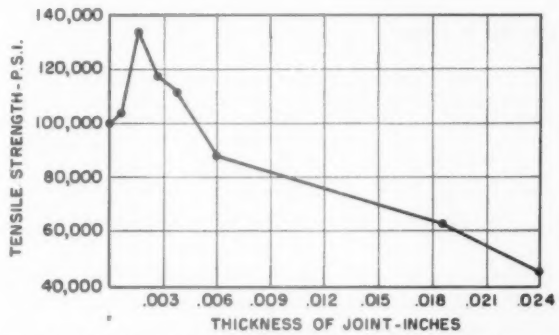

Fig. 1 Relationship of tensile strength to joint clearance of silver-copper-cadmium-zinc (Easy Flo) joints in stainless steel, according to R. H. Leach[3]

Thus, for determination of the optimum brazing conditions for a certain combination of base metal and brazing alloy, it is not, in itself, adequate to establish the optimum brazing temperature, as has been done in the past by some authors. Brazing temperature is but one factor among several which influence the strength

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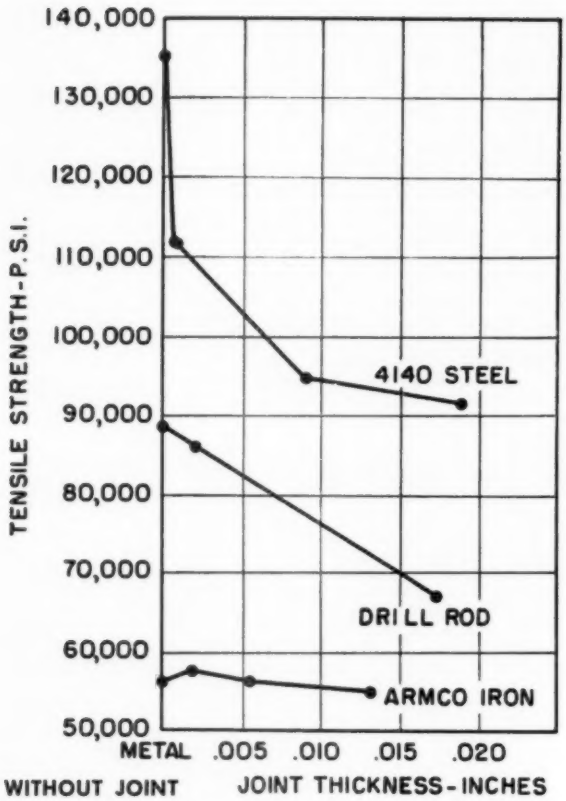


Fig. 2 Effect of joint thickness on the tensile strength of silver-copper-zinc-cadmium (Easy Flo) brazed joints, according to Coxe and Setapen (Handy and Harman report)[1,5]

of the brazed joint. Moreover, for each joint thickness there might be a different optimum brazing temperature.

In a study of soft solders and soldered joints, Nightingale[2] has shown that the optimum soldering temperature depends upon the thickness of the joint. Close fitting joints require a much higher soldering temperature in order to achieve the highest shear strength than relatively loose fitting joints. By plotting joint thickness versus soldering temperature for maximum strength, Nightingale has obtained almost hyperbolic curves. According to Case,[1] studies of brazed joints made with silver alloys have also established a certain relationship between brazing temperature, film thickness and tensile strength of the joint (Fig. 4).

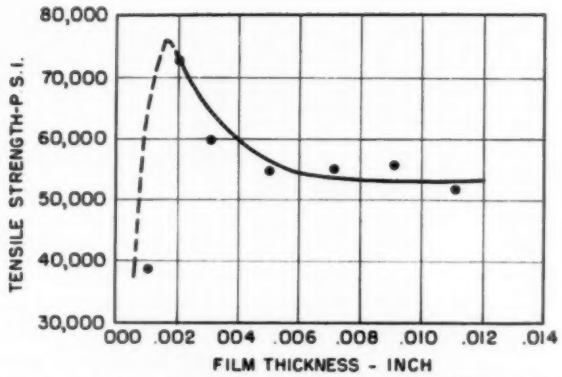


Fig. 3 Relationship between film thickness of filler metal and tensile strength of the joint brazed at 1180° F filler metal—an experimental Ag-Cu-Zn-Cd-Hg-Au-Ni brazing alloy (S. L. Case)[1,6]

Thus the strength of the brazed joint is a function of brazing temperature, time, joint thickness and a great number of more or less important factors, such as surface roughness of the base metal, difference of thermal expansion coefficients of the base metal and brazing alloy, fluxing conditions, yield strength of the base metal, etc.

From the theoretical point of view, the most important relationship, however, is the one between the tensile strength of the brazed joint and the filler metal thickness. As will be shown in the following discussion of this relationship (see Section D of this paper), the theoretical analysis of the pertinent tensile strength-joint thickness curves permits one to broaden and improve considerably the fundamental understanding of

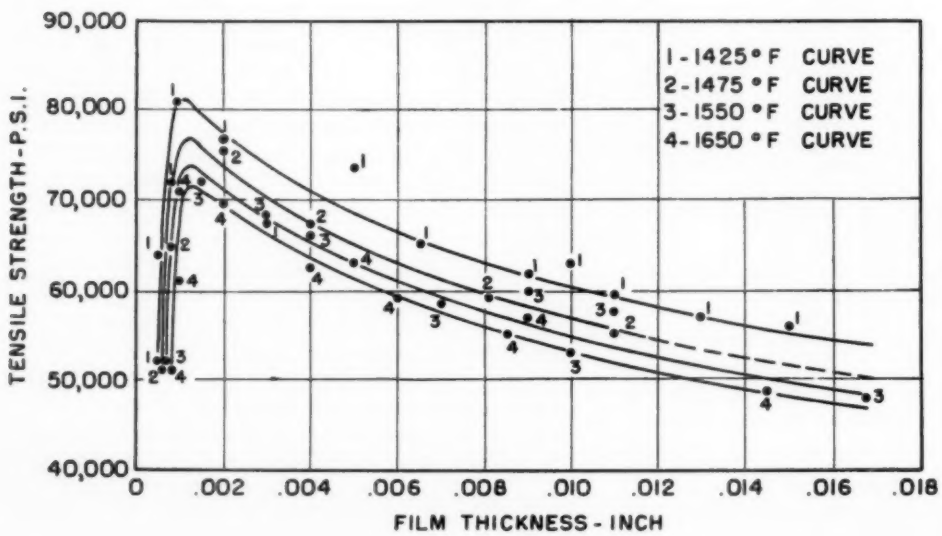


Fig. 4 Relationship between film thickness of filler alloy and tensile strength of joint brazed at various temperatures. Filler alloy—an experimental Ag-Cu-Zn-Ni brazing alloy (S. L. Case)[1,6]

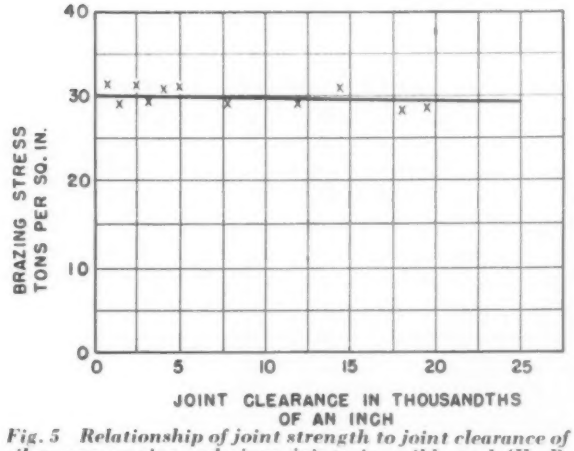


Fig. 5 Relationship of joint strength to joint clearance of silver-copper-zinc-cadmium joints in mild steel (H. R. Brooker and E. V. Beatson)[7]

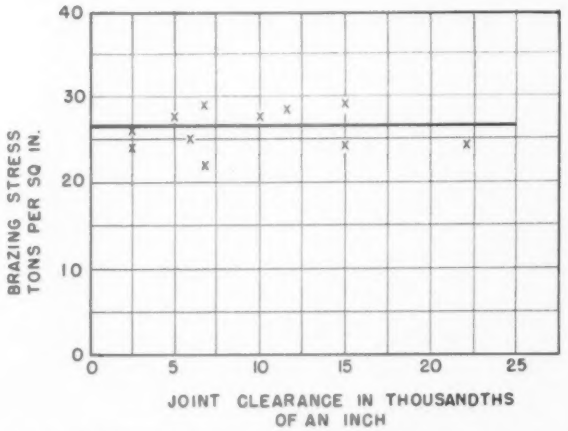


Fig. 6 Relationship of tensile strength to joint clearance of silver-copper-zinc-cadmium joints in mild steel (H. R. Brooker and E. V Beatson)[7]

the factors determining the strength of the brazed joint. Considering the importance of such analysis and the fact that only very few investigations have been done until now in this field, it was decided to start the investigations of the strength of joints with a basic study of the tensile strength-joint thickness curves.

Leach[3] was the first who began to investigate this problem. He has shown that in brazing stainless steel with a quaternary silver alloy, Easy Flo, (50% Ag, 18% Cd, 16.5 Zn, 15.5% Cu) maximum strength of the braze is attained at approximately 0.002 in. film thickness, and that either a decrease or an increase in film thickness materially lowers the strength of the joint (Fig. 1).

More recently the tensile strength-joint thickness curves for Easy Flo brazed joints have been investigated by Coxe and Setapen[4, 5] in the research laboratories of Handy & Harman. In the course of these investigations, Armco iron, drill rod and 4140 steel bars were brazed with Easy Flo, and the pertinent tensile strength-joint thickness curves determined (Fig. 2).

In Handy & Harman Technical Bulletin No. T-3, the question is raised for the first time: Why or how can a silver alloy with an ordinary tensile strength of about, 70,000 psi in a very thin joint withstand a stress of 120,000 psi? The authors point out that this question cannot be answered with certainty. They suggest two theories:

1. "The hard faces of the material being joined prevent or interfere with slip along the crystallographic planes of the brazing alloy.

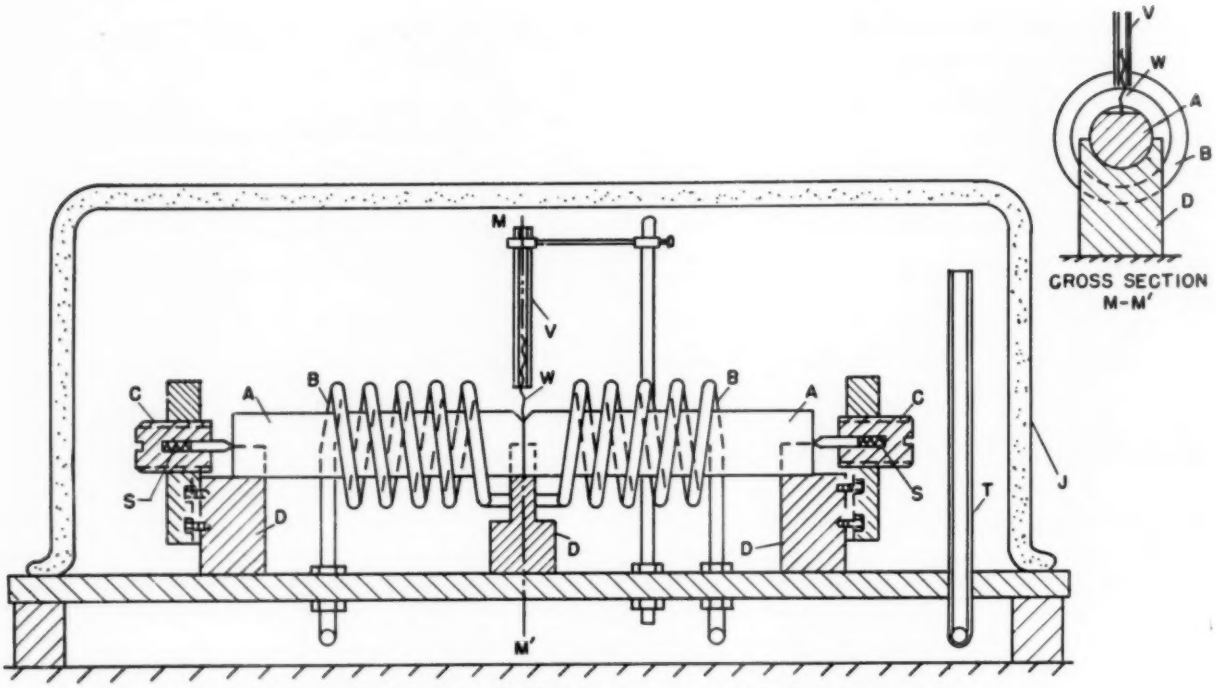
2. When the brazing alloy is stressed, it tends to elongate. However, elongation requires a collateral reduction in area, and this reduction in area is restrained in the joint by closely spaced, hard faces of the steel to which the brazing alloy is bonded. The brazing alloy is then under triaxial tension. It has been established that a material under triaxial tension has a higher breaking strength along the principal axis of stress, and a lower elongation than when pulled in simple tension. When the yield point of steel is reached, the steel elongates and collaterally reduces in cross section. This then means a relaxation of the lateral or radial tension stresses in the joint, thus freeing the brazing alloy to extend out to failure."

Case in his *Monograph on Brazing of Steel*[6] and in a recently published article "Brazing of Steel"[1] presents some tensile strength-joint thickness curves obtained in the Applied Science Research Laboratories of the University of Cincinnati and Battelle Memorial Institute in Columbus, Ohio. These curves were obtained by brazing SAE 4340 steel with various experimental brazing alloys. Two of these curves are illustrated by Figs. 3 and 4.

Case also gives an explanation for the increase of the tensile strength with the decrease of the thickness of the joint. In his opinion, by stressing a very thin joint in a tensile test, the neck-down effect of a relatively plastic brazing alloy is hindered by the rigid steel surfaces. Thus the filler metal film is stressed in a right angle to the direction of the tensile stress. Under the influence of these triaxial stresses the rupture must take place at higher values of tensile stress.

Brooker and Beatson in their book *Industrial Brazing* (1953),[7] also discuss the tensile strength-joint thickness relationship in brazed joints. They have investigated this relationship on silver-copper-zinc-cadmium joints in mild steel, and found in a series of tests that "the ultimate tensile strength of the joint agrees well, over a wide range of joint clearance, with the ultimate strength of the brazing material in the cast condition, which is 30 tons per square inch." In other words, the authors have not obtained a tensile strength-joint thickness curve but just a straight line (Figs. 5 and 6). From these results they conclude that "with the materials employed and under brazing conditions used, the tensile strength of a completely sound joint will be approximately equal to the cast strength of the brazing material, irrespective of the joint clearance."

Referring to the quite impressive tensile strength-joint thickness curve obtained by Leach (Fig. 1), the authors point out that "such results may be exceptional



A-TENSILE SPECIMEN HALF-BARS, B-WATER-COOLED INDUCTION COIL, C-SPACING SCREWS, D-TRANSITE BEARINGS, J-GLASS JAR, S-SPRINGS FOR COMPENSATION OF THERMAL EXPANSION, T-GAS INLET TUBE, V-VYCOR GLASS TUBE FOR SUPPLY OF FILLER METAL WIRE, W-FILLER METAL WIRE.

Fig. 7 Induction heating apparatus for brazing tensile specimen

and may depend upon special phenomena which are not necessarily reproduced on all occasions."

Brooker and Beatson also try to explain why in some cases the tensile strength increases with the decrease of the joint thickness: "As evidenced by R. H. Leach's graph, *special tensile effects and/or alloying** between parent metal and brazing alloy can have a marked effect on strength with certain joint clearances when steel is joined with the silver brazing alloy in question."

This literature survey shows evidently that among the investigators in this field there is at present no unanimity of opinion on the causes of the increase of tensile strength with the decrease of joint thickness. It seems that the lack of agreement is caused chiefly by the fact that the investigated tensile strength-joint thickness curves are obtained on brazing with commercial or experimental brazing alloys which are composed of at least four different metals, some of them even of seven. Certain of these metals may form solid solutions or intermetallic compounds with the base metal, thus further increasing the number of factors which may influence the strength of the brazed joint. Apparently here the research work has been started with the investigation of the most complicated cases without proper investigation of the fundamentals. Under such circumstances, it is quite difficult to avoid confusion and disagreement in the interpretation of the experimental results.

The simplest way to avoid all these complicating influences would be to select for these investigations two mutually insoluble metals, having a large difference in melting points, and without any possibilities of formation of intermetallic compounds. Then, the one with the higher melting point could be used as base metal, and the one with the lower melting point as filler metal.

The most favorable combination of two metals, almost completely satisfying these requirements, is the system Fe–Ag. Plain carbon steel with a melting range from 2500 to 2750° F can be used as base metal, pure silver with a melting point at 1761° F, as filler metal. According to the *Metals Handbook*[8] silver and iron are insoluble in the liquid state and are virtually insoluble in the solid state. At 1000° C (1832° F) only 0.0004% Fe dissolves in silver.

Thus using the iron-silver combination, the influence of the joint geometry on the tensile strength of the joint can be studied without any disturbing influences, and the obtained tensile strength-joint thickness curve would only be a function of mechanical factors depending on the geometry of the joint.

The second combination of metals which could be used for these investigations is the system Fe–Cu. Steel can be used as base metal, and copper (melting point 1981° F) as filler metal. According to the Fe–Cu phase diagram (*Metals Handbook*,[8] p. 1196) iron and copper do not form intermetallic compounds. However, the solubility of iron in copper at 1100° C (2012° F) is 3%. Consequently, the tensile strength of the

* Italics by author.

copper brazed joints might already be influenced by the diffusion of iron into the filler metal. This influence can be eliminated, at least partially, by replacing pure copper by eutectic silver-copper (72% Ag, 28% Cu) brazing alloy. Since the melting point of this alloy is quite low (1435° F), it can be anticipated that at the practical brazing temperature (about 1500° F) the diffusion of iron into the brazing alloy will be negligible. There is one more reason why no appreciable amount of diffusion could be anticipated in these experiments: the high-frequency induction heating method used for brazing of the tensile bars in the course of these experiments permitted a decrease of the total time of one brazing operation (the time of brazing alloy being liquid in the joint was even shorter) to about two to three minutes. The comparatively short brazing time should further decrease the diffusion of iron into the filler metal. Thus under these circumstances the two combinations of metals in question, Fe-Cu and Fe-eutectic Ag-Cu alloy, could be used for the outlined determination of the tensile strength-joint thickness relationship, considering however the fact that the diffusion nevertheless might influence the tensile strength readings.

The fourth combination of metals which could also serve the outlined purposes would be iron as base metal, and 85% Ag + 15% Mn brazing alloy (melting point 1745° F) as filler metal. Since no appreciable diffusion of iron into the filler metal can be anticipated at the comparatively low brazing temperature of this brazing alloy, this combination of metals could also be useful for the outlined investigations.

According to these considerations it has been decided to investigate the tensile strength-joint thickness relationship by brazing low-carbon steel (1020) tensile bars with: (1) pure Ag, (2) pure Cu, (3) eutectic Ag-Cu alloy (BAg8) and (4) 85% Ag + 15% Mn brazing alloy (BAgMn).

B. PREPARATION OF BRAZED TENSILE SPECIMENS

1. Brazing Apparatus

To investigate the tensile strength-joint thickness relationship for the outlined four combinations of metals, it was necessary to find a fast and reproducible method for brazing of tensile specimens, especially considering the fact that faster brazing time decreases the undesirable diffusion of iron into filler metal. Since brazing of the tensile specimen in laboratory furnaces would require quite a long time for a complete brazing cycle (heating, brazing and cooling), it was decided to try to develop a faster method by using high-frequency induction heating. In the course of the experimental work this method finally has been worked out and improved to braze tensile specimens in two to three minutes.

The induction heating apparatus used in this method is essentially an induction coil which consists of two

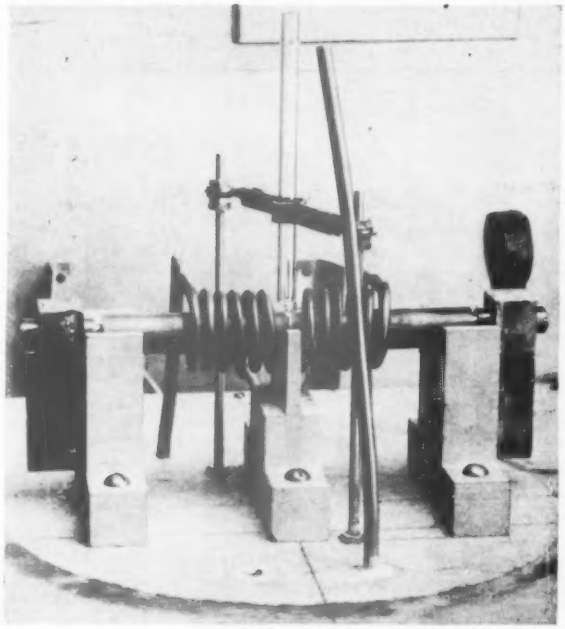


Figure 8

parts (Figs. 7 and 8). Each part of the coil is designed to heat up one half of tensile specimens, *A*, which are placed in the center of the coils, *B*, so that the axes of the specimens coincide with the axis of the coils. The tensile specimens were supported by adjustable transite bearings, *D*. Both coils were mounted symmetrically, left and right from the brazed joint. They were wound from 1/4-in. copper tubing, since they had to be water cooled during the heating process. Each coil consisted of five turns, the inner diameter of the turns being equal to 1 in. The high frequency current was supplied by a 15-kw spark gap generator.

During the brazing experiments it has been observed that the position of the coils in relation to the brazed joint is quite an important factor which might greatly influence the quality of the joint. If the coils are placed too close to the brazed joint, a "void" will be produced in the center of the brazed joint. This results because the outer surface of the bars on both sides of the joint will heat up much faster than the core.

When the molten filler metal flows into the joint from the outside, as was the case in our brazing experiments, the edges of the joint will braze first, thereby leaving a "void" in the center of the joint which is the last portion to come up to temperature.

On the other hand, if the coils are too far away from the joint, then the surface of the bars around the joint will be much cooler than the core because of the thermal distribution resulting from thermal conduction down the bar and thermal radiation from its surface. By proper adjustment of the position of the coils with respect to the joint, a rather uniform temperature throughout the joint can be achieved.

The experience has proved, however, that the most favorable condition for the formation of a perfect joint will be achieved by holding the temperature of the core

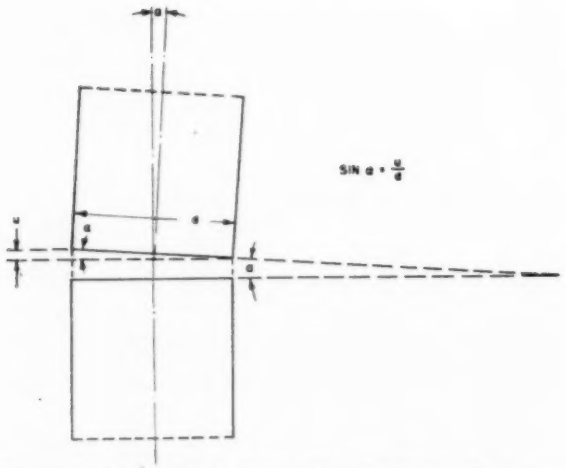


Fig. 9 Relationship between the difference of the two extreme joint thicknesses, "u," and the angle between the axes of the two tensile bars, "a"

slightly above the temperature of the surface. This condition is important during the melting period of the brazing alloy, because it minimizes the possibilities for "void" formation.

By keeping the temperature of the core slightly above the temperature of the surface, and by proper arrangement of the feeding of the brazing alloy into the joint, completely sound and voidless joints with a filler metal thickness from 0.0003 to 0.0150 in. could be achieved for silver brazed joints, from 0.0025 to 0.0130 in. for copper brazed joints, from 0.0035 to 0.0210 in. for BAg8 brazed joints, and from 0.0030 to 0.0130 in. for BAgMn brazed joints. Thus this method was useful for brazing of tensile bars over a wide range of joint thicknesses.

2. *Alignment of the Tensile Bars*

The next step, after the heating problem was successfully solved, was to achieve an accurate alignment of both halves of the specimen during the brazing operation. According to Case,[6] the slightest misalignment causes bending stresses during pulling of the tensile bars and this seriously affects the results.

To obtain a uniform thickness throughout the joint, especially for very thin joints, the brazed surfaces must be accurately finished to a high degree of precision. Furthermore, both surfaces must be quite perpendicular to the axes of the bars. If both these requirements are accurately observed, then the precision of the alignment can be easily determined by measuring the thickness of the filler metal at regular intervals on the periphery of the joint. For example, in one of our experiments, where two 3-in. long round bars, $^3/_4$ in. in diameter, were brazed, an average joint thickness of 0.0016 in. has been achieved. However, on measuring the joint thickness at 90-deg intervals around the brazed joint, the following values were found: 0.0019, 0.0016, 0.0013 and 0.0016 in. Thus in a joint having an average thickness of 0.0016 in., the difference between maximum and minimum values was:

$$u = 0.0019 - 0.0013 = 0.0006 \text{ in.}$$

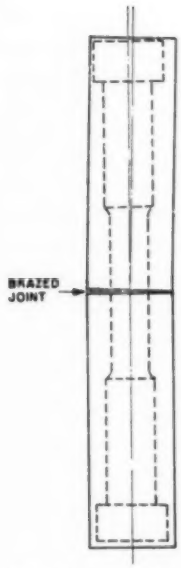


Fig 10 Tensile test bar (dotted lines) turned down from brazed half-bars (continuous lines)

The diameter of the bars was $d = {}^3/_4$ in.; from this value and the joint thickness variation, u, the angle α (Fig. 9), between the two axes of the tensile bar halves can easily be calculated:

$$\sin \alpha = \frac{u}{d} = \frac{0.0006}{0.75} = 0.0008$$

$$\alpha = 0°3'$$

From this calculation it is apparent that the alignment of the two bars can be improved without altering the value of u, simply by increasing the diameter of the tensile bars, d. However, the increase of the diameter of the tensile bars is limited since the difficulties to achieve a sound joint considerably increase with the increase of the diameter of the bars.

Practically, a slight misalignment can be completely corrected by using bars of larger diameter and turning them down after brazing to a smaller final diameter such as that of the standard tensile bars. In this case, both halves will have one common axis and the only imperfection will be a slight variation in thickness of the joint (Fig. 10).

Accordingly, as a standard tensile test procedure for the reported experiments it was decided to use two 3-in. long round bars, $^3/_4$ in. in diameter, and turn them down after brazing to a standard 0.505 in. diam tensile bar specimen.

3. *Supply of Filler Metal into the Joint*

In the course of the brazing experiments, it has been proved that the best joints can be achieved by feeding the joint with molten filler metal from a groove which has been cut over the brazed joint as illustrated by Fig. 11. The groove was cut in the outer layer of the brazed bars, which after brazing had to be turned down on the lathe. The filler metal in the form of wire, $^1/_{16}$ in. diam, was placed over the groove so that one end of the

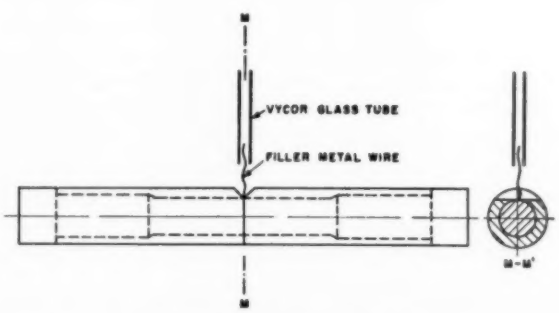


Fig. 11 Feeding of the joint

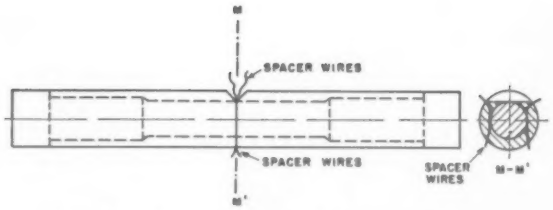


Fig. 12 Proper spacing of the spacer wires

wire rested on the bottom of the groove, just on the edge of the brazed joint. At the moment the temperature of the ends of the bars exceeds the melting point of the brazing alloy, the end of the wire which touches the bottom of the groove starts to melt. The molten brazing alloy flows into the capillary gap and the excess alloy fills the groove over the brazed joint.

For brazing of thicker joints, two or even three filler metal wires had to be placed over the joint to assure an adequate supply of filler metal. In very thick joints, however (0.0150 in. and above for silver, 0.180 in. and above for copper, etc.), no sufficient filling of the joint by molten filler metal could be achieved, even by increased number of wires. Apparently under the specific conditions of this brazing method in joints above a certain upper limit of thickness, the capillary forces were too weak to keep the molten filler metal in the joint.

4. Spacing of the Joint

By using stainless steel wire spacers, a rather uniform joint thickness could easily be achieved in most of the joints. The spacer wires were placed between the edges of the bars, so that they did not occupy any portion of the core of the bars, which, after brazing, had to form the tensile specimen (Fig. 12). In order not to interfere with the capillary forces of the filler metal, the spacer wires should not touch the feeding groove. The best procedure was to place them symmetrically on the left and right of the feeding groove as is illustrated by Fig. 12.

After the spacing wires were properly placed in the joint, the half-bars were pressed together with the help of the spacer screws, *C* (Fig. 7).

During the heating process, the thermal expansion of the half-bars was compensated by the springs, *S*, which were concentrically mounted in the screws, *C*, as illustrated by Fig. 7.

For obtaining very thin joints, no spacer wires were used; in these joints the thickness of the filler metal film presumably was regulated by the pressure of the springs, *S*, during the flow of the molten filler metal into the joint. The pressure of the springs, *S*, on the ends of the half-bars can be regulated by the screws, *C*.

The relationship between the pressure exerted on the ends of the half-bars during the brazing process and the obtained filler metal film thickness was not investigated since the measuring of the amount of pressure exerted on the ends of the half-bars would be a quite complicated task.

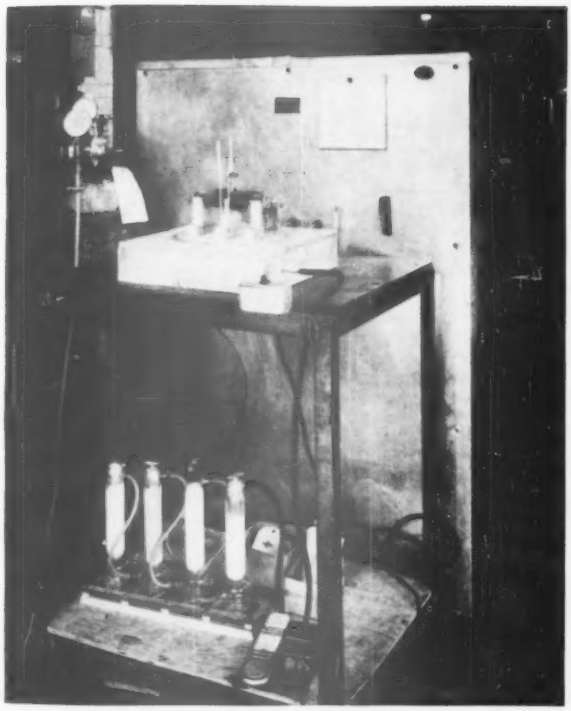


Figure 13

According to the observations made during the brazing experiments, it seems that under a given axial pressure exerted on the ends of the half-bars, the film thickness of the molten filler metal in very thin joints is a function of surface tension, viscosity, and, probably, some other properties of the molten filler metal. Molten silver, for instance, even under considerable pressure exerted on the ends of the half-bars, flows into the gap, forming a continuous film of a finite thickness.

Similar observations have been made by S. L. Case[6] by brazing of vertically placed tensile half-bars which during the brazing operation were subject to pressure exerted by a load placed on the top of the upper half-bar. According to the summary of these experiments: "In the exploratory stage of the experiments, it was believed that control of pressure alone should be sufficient to obtain the desired film thickness, if a fixed relationship between pressure and film thickness was established. It was found, however, that no predictable results could be obtained and the film thickness in all cases when the pressure exceeded 10 lb proved to be less than 0.001 in."

However, as has been emphasized previously in this paper, the brazing alloys used by Case, Leach and others were commercial brazing alloys, composed of four, five or more different metals, and some of these metals could form solid solutions or intermetallic compounds with the iron on the steel-brazing alloy interface. Consequently, it is difficult to compare the results of these experiments with those obtained at the Armour Research Foundation.

Table 1—Tensile Test Results of Low-Carbon (1020) Steel Bars Brazed with Pure Silver Under 10% H_2 + 90% N_2 Atmosphere

Specimen No.	Bar diam,* in.	Joint thickness, in. 0°	90°	180°	270°	Avg	Fracture load, lb	UTS, psi	Quality of the metallic bond
23A	0.4861	0.0115	0.0063	0.0040	0.0075	0.0073	6,850	36,900	Some voids (10%)
24A	0.4498	0.0090	0.0063	0.0055	0.0030	0.0072	6,200	39,000	Some voids (15%)
25A	0.4972	...	...	...	...	0.0005†	12,200	62,900	Absolutely sound and voidless
26A	0.5041	0.0043	0.0033	0.0043	0.0040	0.0040	10,150	50,900	Some voids (10%)
27A	0.5046	0.0075	0.0043	0.0063	0.0070	0.0063	7,920	39,000	Almost voidless (5%)
28A	0.5052	0.0093	0.0085	0.0085	0.0083	0.0086	8,275	41,300	Almost voidless (2%)
30A	0.5046	0.0155	0.0153	0.0143	0.0145	0.0149	4,680	23,400	Many voids (20%)
31A	0.5019	0.0185	0.0168	0.0183	0.0175	0.0178	3,000	15,100	Many voids (20%)
ALC 128	0.5067	0.00051	0.0000	0.00012	0.00054	0.00029‡	13,550	67,200	Absolutely sound and voidless

* Average of five readings.
† Measured after fracture.
‡ Measured under a special high-power microscope.

5. *Protective Atmosphere*

Experience obtained during the brazing experiments has proved that by using high-frequency induction heating for brazing of tensile test bars, the best joints could be achieved under reducing atmosphere without any fluxes or cleaning agents. A commercial gas, 10% H_2 + 90% N_2, purchased in cylinders has been tested and chosen as the safest and yet most effective for brazing of tensile bars by induction heating methods. This gas is not explosive, the price of it is comparatively low, and the percentage of water vapor (dew point: −30° F) and oxygen are usually negligible so that no purification or drying of the gas are necessary.

According to the *Metals Handbook*,[8] page 294, at 2000° F the equilibrium ratio H_2O/H_2 for the $FeO + H_2 \rightleftharpoons Fe + H_2O$ reaction is 0.75. In the case of 10% H_2 + 90% N_2 gas this means that the critical water content is 7.5%, or, if the water vapor percentage is below 7.5 the gas will reduce iron oxide on the steel surface; if the percentage is above 7.5 the gas will oxidize the steel surface. Since according to the dew point determinations the commercial 10% H_2 + 90% N_2 gas has only 0.25% H_2O, it is quite obvious that the gas will reduce iron oxides at temperatures far below the liquids temperatures of silver, copper and their alloys.

The experimental results confirmed this reasoning: under 10% H_2 + 90% N_2 atmosphere, very sound, voidless joints could be achieved by brazing steel with pure silver, pure copper and BAgMn brazing alloy. Considerable difficulties, however, have been encountered by brazing with BAg8 brazing alloy, presumably due to the low melting point of this alloy, and consequently, insufficient reducing of the steel surfaces.

Figure 13 represents the general view of the induction brazing apparatus with all the auxiliary devices. The 10% H_2 + 90% N_2 gas cylinder can be seen on the left; below the table are placed four gas drying towers which were used in the exploratory stage of the experiments before it was realized that the moisture content in the gas was so low that drying was absolutely unnecessary. The spark-gap high-frequency generator is in the background of the picture. In the center of the picture is the induction heating apparatus, placed on a table. For the sake of easier viewing, the glass jar is removed from the apparatus.

C. EXPERIMENTAL RESULTS

1. *Classification of Experimental Data*

The results of the tensile tests and joint thickness measurements are summarized in Tables 1, 2, 3 and 4. Table 1 shows the results obtained on silver brazed joints; Table 2, on copper brazed joints; Table 3, on

Table 2—Tensile Test Results of Low-Carbon (1020) Steel Bars Brazed with Pure Copper Under 10% H_2 + 90% N_2 Atmosphere

Specimen No.	Bar diam,* in.	Joint thickness, in. 0°	90°	180°	270°	Avg	Fracture load, lb	UTS, psi	Quality of the metallic bond
2A	0.5123	0.0032	0.0015	0.0000	0.0000	0.00012	9,925	48,200	One sector (20%) not covered; rest no voids
3A	0.4955	0.0051	0.0000	0.0025	0.0028	0.0026	9,925	51,500	Absolutely sound and voidless
4A	0.4976	0.0053	0.0043	0.0050	0.0041	0.0047	8,200	42,200	Many small voids (5%)
5A	0.4505	0.0090	0.0063	0.0075	0.0070	0.0074	6,850	43,000	Some voids (5%)
7A	0.4955	0.0098	0.0095	0.0082	0.0097	0.0093	4,540	23,600	Voids cover 60% of brazed surface
8A	0.4700	0.0220†	0.0095	0.0113	0.0095	0.0101	5,100	29,400	Large voids (25%)
9A	0.4650	0.0145	0.0138	0.0110	0.0128	0.0130	7,250	42,700	Some voids (5%)
10A	0.5052	0.0142	0.0141	0.0128	0.0135	0.0136	7,250	36,200	Some voids (10%)†
11A	0.5076	0.0205	0.0173	0.0163	0.0190	0.0183	8,100	39,900	One large void (10%)
CLC 196	0.5010	Not measurable					13,800	70,000	Broke in base metal

* Average of five readings.
† This value is out of the limits of error and is not included in the average. Apparently it is caused by the feeding groove traces in the joint.

Table 3—Tensile Test Results of Low-Carbon (1020) Steel Bars Brazed with BAg8 Brazing Alloy Under 10% H_2 + 90% N_2 Atmosphere

Specimen No.	Bar diam,* in.	Joint thickness, in. 0°	90°	180°	270°	Avg	Fracture load, lb	UTS, psi	Quality of the metallic bond
1A	0.4985	0.0085	0.0080	0.0070	0.0075	0.0077	8,120	41,000	Separation on the interfaces
32B	0.5017	0.0015	0.0010	0.0007	0.0000	0.0008	8,750	44,100	Almost voidless (5%)
33B	0.5078	0.0040	0.0040	0.0028	0.0030	0.0034	12,200	60,300	No voids. Separation on the interfaces
34B	0.5055	0.0083	0.0073	0.0073	0.0075	0.0076	8,525	42,600	No voids. Separation on the interfaces
35B	0.5057	0.0090	0.0073	0.0080	0.0083	0.0081	9,200	45,800	No voids. Separation on the interfaces
36B	0.5045	0.0095	0.0093	0.0090	0.0090	0.0092	7,950	39,800	No voids. Separation on the interfaces
37B	0.5006	0.0155	0.0155	0.0150	0.0138	0.0150	8,250	41,900	Some voids. Separation on the interfaces
38B	0.5063	0.0250	0.0230	0.0138	0.0173	0.0210	9,100	45,200	Separation on the interfaces

* Average of five readings.

Table 4—Tensile Test Results of Low-Carbon (1020) Steel Bars Brazed with BAgMn Brazing Alloy Under 10% H_2 + 90% N_2 Atmosphere

Specimen No.	Bar diam,* in.	Joint thickness, in. 0°	90°	180°	270°	Avg	Fracture load, lb	UTS, psi	Quality of the metallic bond
39C	0.5076	0.0025	0.0010	0.0005	0.0005	0.0011	10,360	51,300	Some small voids (5%)
40C	0.5072	0.0035	0.0030	0.0028	0.0030	0.0031	11,500	57,700	Almost perfect (2%)
41C	0.5101	0.0083	0.0038	0.0028	0.0065	0.0055	9,040	44,300	Some voids (10%)
42C	0.5022	0.0128	0.0095	0.0073	0.0065	0.0090	7,525	38,000	Separation on the interfaces
43C	0.5046	0.0155	0.0113	0.0128	0.0133	0.0132	10,400	52,000	Perfect joint
44C	0.4961	0.0203	0.0190	0.0138	0.0175	0.0189	62,500	37,500	Large voids

* Average of five readings.

BAg8 brazed joints; and Table 4, on BAgMn brazed joints. Table 5 shows the tensile strength of low-carbon (1020) steel bars, heated and cooled in the induction coil, under exactly the same conditions as in a standard brazing cycle.

Table 5—Tensile Test Results of Low-Carbon (1020) Steel Bars* (Without Brazed Joint)

Specimen No.	Bar diam,† in.	Fracture load, lb	UTS, psi
48	0.5031	14,300	72,000
49	0.5042	12,400	62,100
50	0.5046	14,300	71,500
		Avg =	69,000

* The bars were heat treated in the induction coil under exactly the same conditions as in a standard brazing cycle.
† Average of five readings.

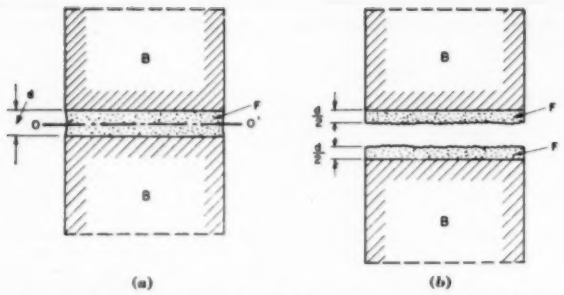
In these tables, the first column shows the number of the tensile specimens (bars). In the second column is given the diameter of the tensile bars in inches, computed as an average result of five readings (the first reading made just on the joint; the second and third, $^1/_4$ in. left and right of the joint, respectively; and the last two readings, $^1/_2$ in. left and right of the joint).

The third column shows four filler metal thickness readings in inches made with the help of a portable microscope on the surface of the tensile bars at 90-deg intervals around the joint. The average of these four readings is shown in the column entitled "average joint thickness."

The fourth column shows the load of fracture in pounds which in all tests was equal to the maximum load. The pulling of the brazed tensile specimen has been performed under standard tensile conditions, with a pulling rate of about $^1/_8$ ipm.

The fifth column shows the ultimate tensile strength of the brazed joint in pounds per square inch. In the case of brazed tensile bars, ultimate tensile strength is almost equal to the "true" strength of the joint, since, according to the measurements of the diameter of the joints made after fracture, the neck-down effect of the filler metal disk in all cases was quite negligible.

The sixth column—the quality of the metallic bond—is reserved for the characteristics of the appearance of the filler metal after fracture on both half-bars of the tensile specimen. By investigation of the fractured joints, it has been observed that only in comparatively few cases the joints were absolutely sound and voidless. It was found that the highest tensile strength values were produced by joints which were (1) voidless and (2) broke in tension in the middle of the filler metal disk, so that after fracture the thickness of the filler metal layers on both half-bars was about the same (Fig. 14). In such completely perfect joints the plane of fracture was identical with the plane of symmetry of the joint. These joints have been designated as "absolutely sound and voidless" and their tensile strength values have been selected for the construction of the tensile-strength joint thickness curves. Figure 15 shows an enlarged photograph of a sample of an "absolutely sound and voidless" joint (sample ALC 128).



(a) (b)

Fig. 14 Cross section of an "absolute sound and voidless" brazed joint

(a) Before fracture; (b) after fracture. *B*, base metal; *F*, filler metal; 0-0', plane of fracture.

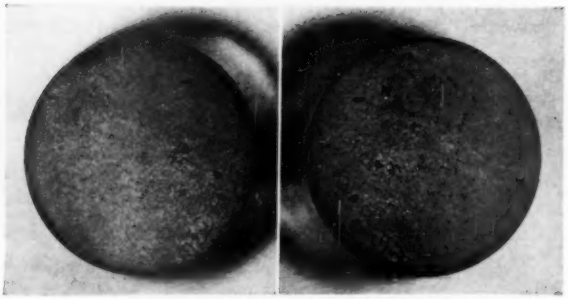


Fig. 15 An enlarged photograph of a fractured brazed joint (sample ALC 128). The two tensile half-bars were brazed with pure silver. The filler metal on both half-bars is absolutely voidless and without any imperfections

2. *Imperfections of the Metallic Bond and Their Influence on the Tensile Strength of the Joint*

Joints which had a large number of voids, or joints which by pulling did not separate in the middle but completely or partly on the base metal-filler metal interfaces, yielded in all cases comparatively small tensile strength values.

However, the joints with a small number of voids—not exceeding 10% of the total area of the brazed surface—yielded quite high tensile strength values, sometimes just as high as the voidless joints. Only the joints with an area of voids exceeding 10% of the total brazed surface yielded considerably lower tensile strengths. Accordingly, the tensile strength values of joints with few voids have also been used for the construction of the tensile strength-joint thickness curves; for example, the tensile strength values of samples 5A, 26A, etc. An estimated percentage of the area covered by voids for the fractured samples is given in the last column in parentheses.

Since the tensile bars have been brazed under a reducing atmosphere, without any fluxes or cleaning agents, no other types of voids could be anticipated in the filler metal except gas inclusions. A thorough investigation of the fractured joints confirmed this reasoning: only in very few cases have some solid, nonmetallic inclusions been observed, which apparently were caused by some remains of iron oxides which were not sufficiently reduced by hydrogen. However, in the majority of the fractured brazed joints, only some variable numbers of gas inclusions and no other imperfections have been observed in the filler metal. The number and size of gas inclusions increased with the joint thickness. Extremely thin joints had no gas bubbles at all. Thicker joints always had some gas inclusions. In the extremely thick joints, the number and size of gas bubbles were so large that the tensile strength values were no longer dependable.

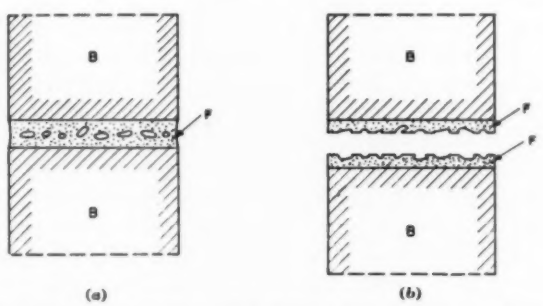
On the other hand, the large number of gas inclusions was not the only reason why sound joints could not be achieved in extremely wide gaps. Undoubtedly, the weakness of capillary forces was another factor which caused insufficient filling of the extremely thick gaps by molten filler metal, thus also contributing to the weakness of the extremely thick joints.

Because of the weakness of such joints, it was impossible to determine exactly the tensile strength-joint thickness relationship for extremely thick joints. The extensions of the tensile strength-joint thickness curves in the region of very thick joints are extrapolations to the tensile strength of the braze metal. The extrapolated part of these curves is marked by dotted lines (Figs. 20, 21, 22 and 23).

Only in very few cases, excessively large voids have been observed in joints of medium thickness. These voids usually extended throughout the entire thickness of the filler metal layer from one base metal surface to the opposite one, interrupting the filler metal layer at a certain spot and forming in the filler metal disk a large hole. Apparently these large voids are caused by nonwetting of a particular spot on the base metal surface due to insufficient reduction or cleaning of the base metal surface, or—as is the case in extremely thick joints—due to the weakness of the capillary forces. Consequently these large voids are caused by gas entrapments rather than by gas separation.

However, in most of the investigated joints, the gas bubbles were quite small, and—what is particularly characteristic for this type of void—the gas bubbles were concentrated almost in one geometrical plane; namely, the plane of symmetry of the filler metal disk (Fig. 16).

Since by pulling the brazed bars the rupture almost always occurred along the plane of symmetry of the joint, the gas bubbles were opened, and after the frac-



(a) (b)

Fig. 16 Cross section of a brazed joint with small gas inclusions concentrated along the plane of symmetry of the joint

(a) Before fracture; (b) after fracture. *B*, base metal; *F*, filler metal.

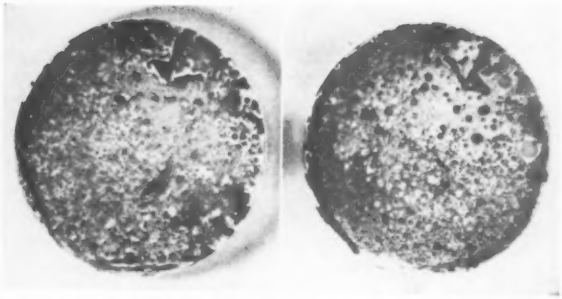


Fig. 17 An enlarged photograph of a fractured brazed joint (sample 27A). The two tensile bars have been brazed with pure silver. The traces of small bubbles symmetrically distributed are visible on both surfaces

ture the separated half spheres of the gas bubbles could be observed as small holes symmetrically distributed on both sides of the fractured joint. Figure 17 shows a photograph of a fractured brazed tensile bar with traces of small gas bubbles distributed symmetrically on both sides of the fractured joint (sample 27A).

The fact that the gas bubbles were concentrated along the plane of symmetry of the brazed joint, that is, in equal distances from both base metal surfaces, indicates that the gas bubbles obviously were formed by separation of a gas or gases from the molten filler metal after brazing. By cooling the tensile half-bars after brazing, the filler metal layer along the plane of symmetry is the last portion of the filler metal disk which is still liquid at the time when the rest of the metal in the layers adjacent to the cooling base metal surfaces is already solid. Consequently, the only place where the segregation of dissolved gases can occur is the last portion of the filler metal which is liquid, and that is the plane of symmetry.

The exact mechanism of the formation of these small gas bubbles, as well as the nature of the gas or gases has not been completely investigated as yet. It is probable that the small gas inclusions were caused by hydrogen adsorbed by the molten filler metal from the protective gas. The protective gas used for the brazing experiments consisted of 10% H_2 + 90% N_2. Nitrogen is completely insoluble in copper as well as in silver. Hydrogen, however, is soluble in appreciable amounts in molten copper and to a lesser extent in molten silver. By solidification of such solutions the separation of hydrogen takes place and may cause gas inclusions. On the other hand, it may be that the pure metals used (Ag and Cu) were not absolutely free of oxygen; in such a case, hydrogen would react with the oxygen and form steam bubbles.

The small gas bubbles, evenly scattered along the plane of symmetry, have been found exclusively in the joints brazed by pure copper and pure silver, but not in BAg8 or BAgMn brazed joints. The joints brazed with BAg8 and BAgMn in some cases had large gas inclusions apparently caused by gas entrapments.

The second type of imperfections observed in the fractured brazed joints was the failure of the base metal-filler metal interfaces. By the presence of this

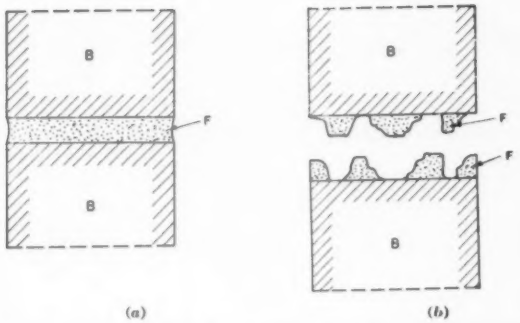


Fig. 18 Cross section of a brazed joint with imperfections on the base metal-filler metal interfaces

(a) Before fracture; (b) after fracture. *B*, base metal; *F*, filler metal.

type of imperfection, the brazed joints did not fracture along the plane of symmetry of the joint but chiefly along the base metal-filler metal interfaces, as illustrated by Fig. 18.

This type of fracture yielded quite low tensile strengths. The fracture along the base metal-filler metal interfaces has been observed exclusively on BAg8 brazed joints, and not in copper, silver or BAgMn brazed joints.

Since at the comparatively low melting temperature of the eutectic copper-silver brazing alloy, BAg8, the rates of the oxide reducing reactions in the 10% H_2 + 90% N_2 atmosphere are much lower than at the melting point of pure copper or silver, or even of BAgMn alloy, it is quite possible that at the time the BAg8 alloy started to melt and fill the capillary joint, the steel surfaces in the joint were not sufficiently reduced by hydrogen and were still covered by oxide films. Thus the failure of the steel-BAg8 interface might be explained by insufficient reducing of oxide films resulting in a weak metallic bond. The confirmation of this hypothesis by metallographic investigations of this type of joint failure has not been completed yet.

Figure 19 shows an enlarged photograph of a fractured tensile bar brazed with BAg8 (sample 36B). It clearly shows the separation on the base metal-brazing alloy interface.

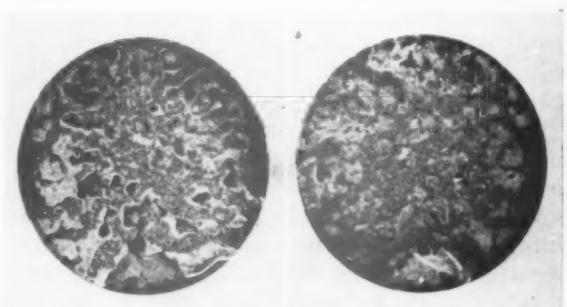


Fig. 19 An enlarged photograph of a fractured brazed joint (sample 36B). The two tensile bars have been brazed with BAg8 brazing alloy. The separation of the base metal-brazing alloy interfaces can be seen on both sides

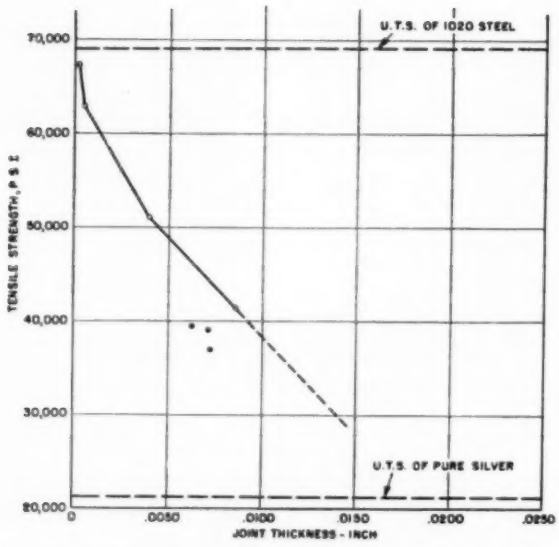


Fig. 20 Relationship of tensile strength to joint thickness for pure silver joints in 1020 steel. Ultimate tensile strength of annealed pure silver—21,000 psi.[9] Ultimate tensile strength of 1020 steel, 69,000 psi

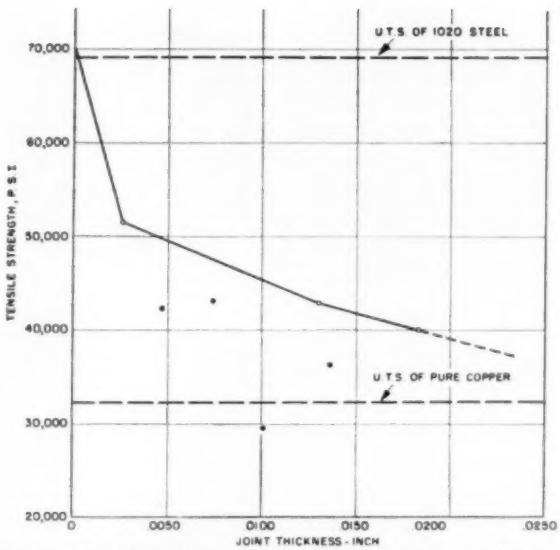


Fig. 21 Relationship of tensile strength to joint thickness for pure copper joints in 1020 steel. Ultimate tensile strength of annealed pure copper—32,000 psi.[9] Ultimate tensile strength of 1020 steel—69,000 psi

3. *Brazing Temperature*

Some of the temperature measurements have been made during the brazing experiments. The thermocouple was placed in a hole drilled radially in the side surface of the bar, quite close to the capillary gap. The temperature measurements showed that as soon as the temperature in the region of the joint slightly exceeded the melting point of the filler metal, the filler metal wire began to melt and almost instantly filled the joint (Fig. 11). In all the brazing experiments the same standard procedure was strictly observed: five to ten seconds after filling of the joint by molten filler metal, the high-frequency heating was cut off. At that time, the temperature of the bars around the joint achieved the maximum value, stayed at the maximum mark for about twenty seconds and then slowly began to drop down. Since the described standard brazing procedure was observed during brazing of all the tensile bars, it can be assumed that all tensile specimens brazed with a certain filler metal were brazed under the same temperature conditions, although the brazing temperature during the brazing process was not quite constant.

As mentioned previously, in order to exclude any diffusion of iron into the molten filler metal, the brazing time had to be cut down to a minimum. Certainly, under such circumstances, precise temperature measurements could not be carried out. It was observed, however, that during the filling of the joint by molten filler metal, the temperature around the joint rises to about 20–40° F above the melting point of the filler metal, stays for a while at that reading, and then starts to decrease slowly.

Since precise measurements of the brazing temperature were not feasible, and since, according to Case,[1, 6] slight variations of the brazing temperature do not appreciably affect the tensile strengths of the brazed joints (Fig. 4), it was decided not to make special temperature measurements during brazing of the tensile bars.

4. *The Tensile Strength-Joint Thickness Curves*

From the experimental results given in Tables 1, 2, 3 and 4, and from the discussion of the imperfections observed on fractured joints brazed by the induction heating method, it is quite obvious that the induction heating method is far from perfect. Only comparatively few joints were absolutely sound and voidless and yielded the highest tensile strength values.

However, the number of such sound, or almost sound, joints was sufficiently high for construction of the tensile strength-joint thickness curves. Since only absolutely perfect joints and joints with the area of voids not exceeding 10% of the total brazed area—which also yielded the highest, or almost the highest, tensile strengths—have been selected for the construction of the tensile strength-joint thickness curves, these curves are considered to represent the tensile strength-joint thickness relationship for the ideally prepared joints.

The maximum tensile strength values of the brazed joints for a certain base metal-filler metal combination must be independent of the brazing method, provided that:

1. The filler metal disks are absolutely sound and voidless.
2. The filler metal disks have no signs of diffusion or intermetallic compound formation.
3. The filler metal disks are not distorted and have a continuous thickness.
4. The fracture zones are placed along the plane of symmetry.

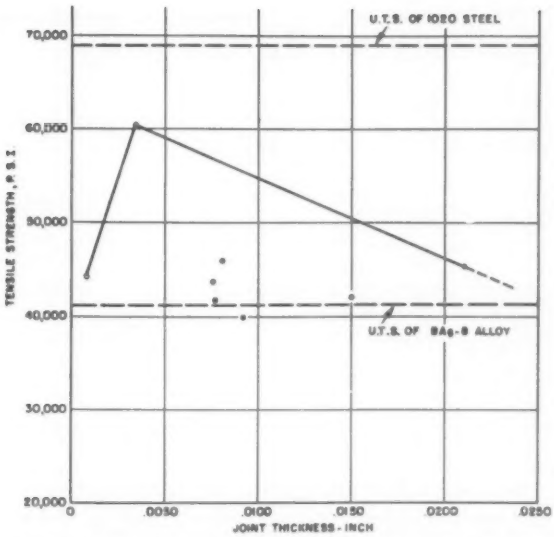


Fig. 22 Relationship of tensile strength to joint thickness for BAg-8 joints in 1020 steel. Ultimate tensile strength of annealed BAg-8 alloy—41,000 psi.[9] Ultimate tensile strength of 1020 steel—69,000 psi

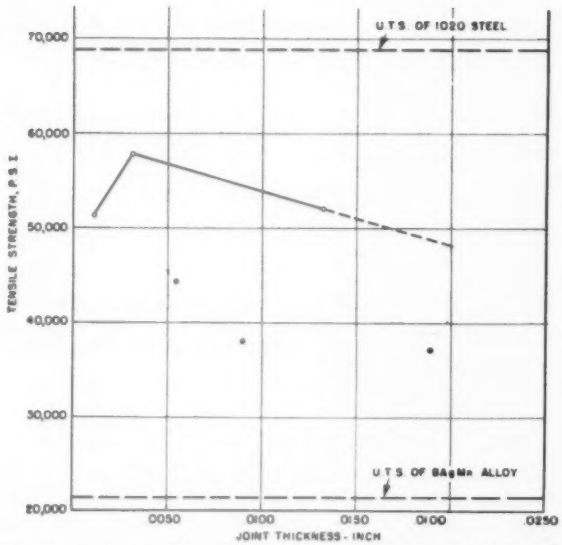


Fig. 23 Relationship of tensile strength to joint thickness for BAg Mn joints in 1020 steel. Ultimate tensile strength of annealed BAg Mn alloy—21,000 psi.[9] Ultimate tensile strength of 1020 steel—69,000 psi

5. The brazing temperature is not appreciably altered.

6. The base metal bars have all the same mechanical properties.

7. The alignment of the tensile bars is perfect.

Accordingly, the results reported for butt-brazed tensile test pieces should be considered applicable to other brazed joint geometries provided the above-mentioned requirements are fulfilled.

Figure 20 represents the tensile strength-joint thickness relationship for joints brazed with pure silver; Fig. 21, for joints brazed with pure copper; Fig. 22, for joints brazed with BAg8 brazing alloy; and Fig. 23, for joints brazed with BAgMn brazing alloy.

Below these curves are marked also the tensile strength values of the joints with voids or some other imperfections which did not yield the highest tensile strength values. The horizontal lines below the curves represent the ultimate tensile strength of the pertinent filler metal in annealed state.

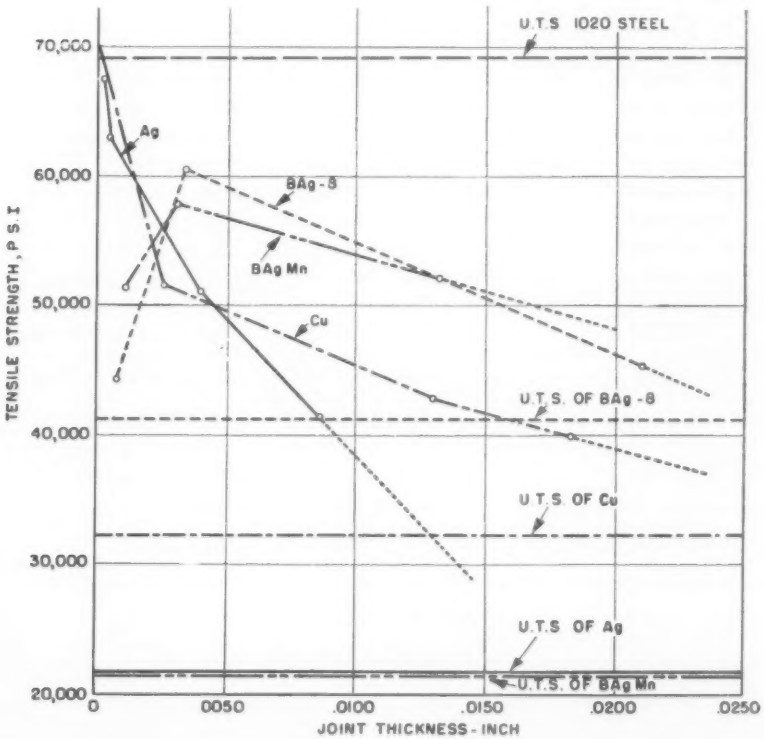


Fig. 24 Tensile strength-joint thickness curves for: (1) pure silver, ——; (2) pure copper, — — —; (3) pure BAg-8, — — —; (4) BAg Mn-alloy, —·— alloy in 1020 steel

Table 6—Tensile Test Results of Drill Rod Bars Brazed with Pure Silver Under 10% H_2 + 90% N_2 Atmosphere (After Brazing the Drill Rod Bars Were Cooled Down in 10% H_2 + 90% N_2 Atmosphere)

Specimen No.	Bar diam,* in.	Joint thickness, in. 0°	90°	180°	270°	Avg	Fracture load, lb	UTS, psi	Quality of the metallic bond	Rockwell† hardness number (R_c) of the steel bars
ADR 180	0.5040	0.0115	0.0117	0.0122	0.0113	0.0117	8,850	44,150	Shrinkage cavity (5%)	I—32.5 II—31.5
ADR 194	0.5074	0.00580	0.00580	0.00406	0.00613	0.00545	11,750	58,150	Some gas entrapments	I—26.0 II—26.5
ADR 114	0.5052	0.00025	0.00149	0.00221	0.00080	0.00119	15,350	76,600	Almost perfect joint (2% voids)	I—33.5 II—33.5
ADR 115	0.5052	0.00101	0.00052	0.00075	0.00014	0.00060	16,865	84,200	Extremely thin film. Somewhat thicker at edges	I—34.5 II—31.5
ADR 116	0.5052	0.00055	0.00001	0.00000	0.00002	0.00014	16,900	84,350	Extremely thin film. Somewhat thicker at edges	I—32.5 II—36.5

* Average of five readings.
† Rockwell hardness measurements have been made at 1 mm distance from the joint, on both sides of it. Average hardness: $R_c = 32$.

The horizontal lines above the curves represent the ultimate tensile strength of the SAE 1020 steel. The average ultimate tensile strength of the unbrazed SAE 1020 steel bars, heated and cooled under the same conditions as in the standard brazing process has been determined (see Table 5) and was found to be equal to 69,000 psi. Since the heat-treating conditions were equal, it can be assumed that 69,000 psi represents also the average tensile strength of the steel in the brazed SAE 1020 steel bars.

For the sake of easier comparison all four curves are plotted in one diagram in Fig. 24.

5. *Tensile Strength of Brazed Drill Rod Bars*

In order to prove that the tensile strength of a brazed joint of a certain thickness is not only determined by the mechanical properties of the filler metal but also by the mechanical properties of the base metal, it was decided to braze some drill rod bars (plain carbon steel with about 1.0 to 1.1% C) with pure silver and pure copper and to determine the tensile strength of these joints. The results of these determinations are given in Tables 6 and 7. From the tensile strength-joint thickness curves investigated by Coxe and Setapan (see Fig. 2) it is quite obvious that for a given joint thickness, all other conditions being equal, the tensile strength of the joint increases with the increase of the tensile strength of the base metal. As these curves show, in infinitely thin brazed joints, the tensile strength of the joint approaches the tensile strength of the base metal.

Consequently, for precise determination of the tensile strength-joint thickness relationship, and in order to obtain comparable results, it is absolutely necessary to measure not only the tensile strength and the joint thickness, but also the hardness of the base metal in the layers adjacent to the brazed joint, since the hardness of these layers influences the strength of the joint.

Accordingly, in Tables 6 and 7 one column more was added at the end of the tables: a column showing the Rockwell hardness number, R_c, of the drill rod bar layers adjacent to the brazed joint. These hardness measurements were made on the surface of the brazed tensile bars on both sides of the joint, at 1 mm. distance from it. The hardness measurements were made before the tensile tests, just after turning down the brazed bars to 0.505 in. diam. Since all the brazed drill rod bars were heated and cooled in 10% H_2 + 90% N_2 atmosphere under exactly the same conditions there was not much variation in the hardness of the steel. For instance, the minimum hardness of the silver brazed drill rod bars was $R_c = 26.0$ (sample ADR 114), and the maximum hardness, $R_c = 36.8$ (sample ADR 140). The average of these hardness measurements was $R_c = 32$.

From the measured hardness number can be found, through use of the appropriate conversion charts for plain carbon steel, the ultimate tensile strength and the yield strength of the base metal (in layers adjacent to the brazed joint).

Table 7—Tensile Test Results of Drill Rod Bars Brazed with Pure Copper Under 10% H_2 +90% N_2 Atmosphere (After Brazing the Drill Rod Bars Were Cooled Down in 10% H_2 + 90% N_2 Atmosphere)

Specimen No.	Bar diam,* in.	Joint thickness, in. 0°	90°	180°	270°	Avg	Fracture load, lb	UTS, psi	Quality of the metallic bond	Rockwell† hardness number (R_c) of the steel bars
CDR 232	0.4908	0.00349	0.00460	0.00455	0.00400	0.00415	15,700	83,000	Absolutely sound and voidless	I—35.0 II—36.0
CDR 234	0.4858	0.00975	0.01030	0.0130	0.01020	0.01015	13,800	74,500	Absolutely sound and voidless	I—37.0 II—38.0
CDR 246	0.5032	0.00615	0.00800	0.0150	0.00880	0.00836	18,290	92,000	Absolutely sound and voidless	I—36.0 II—36.0
CDR 249	0.5000		Not measurable			0.00000	20,900	106,500	Broke in the base metal	I—36.0 II—37.0

* Average of five readings.
† Rockwell hardness measurements have been made at 1 mm distance from the joint, on both sides of it. Average hardness: R_c = 36.0.

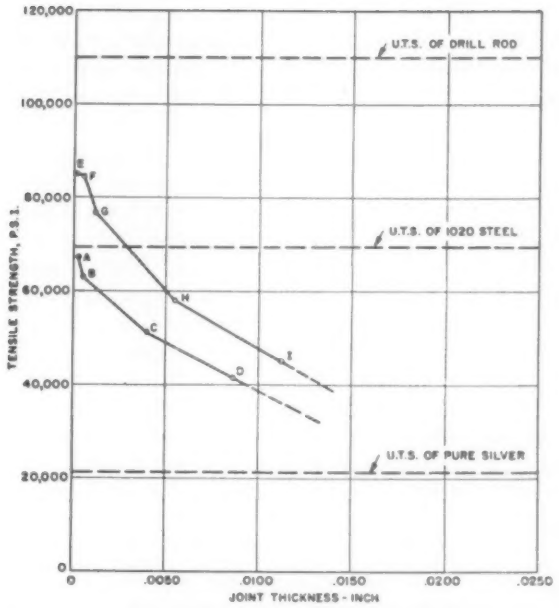


Fig. 25 ABCD, tensile strength-joint thickness curve of pure silver joints in 1020 steel. EFGHI, tensile strength-joint thickness curve of pure silver joints in drill rod bars

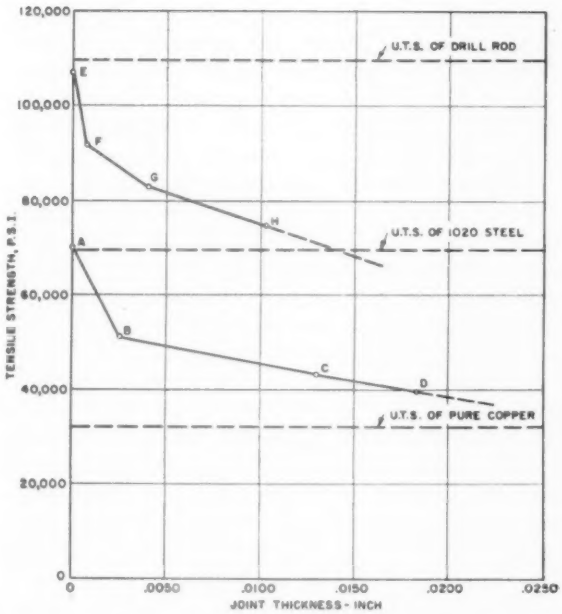


Fig. 26 ABCD, tensile strength-joint thickness curve of pure copper joints in SAE 1020 steel. EFGH, tensile strength-joint thickness curve of pure copper joints in drill rod bars

The exact determination of the tensile strength of the unbrazed drill rod bars, heated and cooled under exactly the same conditions as in standard brazing operations, disclosed an average of 109,500 psi.

However, the determination of the hardness distribution on the surface of the brazed drill rod bars has shown that the variation of hardness on the different sections of the brazed drill rod bars is considerable, presumably due to the fact that through heating in the induction coil during the brazing operation the center section of the tensile bar, adjacent to the brazed joint, is heated to a much higher temperature than the ends of the bar. Since the fracture strength of a brazed joint is determined by the hardness, or, more correctly, by the yield strength of the base metal layer adjacent to the brazed joint, it was decided to measure the hardness of the steel as close as possible (at 1 mm. distance) to the brazed joint, and to consider this obtained hardness number as the most characteristic for the hardness of the steel around the brazed joint.

In order to be able to compare the tensile strength-values obtained on drill rod joints with the corresponding tensile strength values obtained in 1020 steel joints, the tensile strength-joint thickness curves obtained on SAE 1020 steel are reproduced again in Figs. 25 and 26, respectively, and above these curves are plotted the tensile strength-joint thickness curves obtained with the same filler metal in drill rod bars.

As the diagrams show, in both cases the tensile strength values obtained in drill rod bars are much higher than the pertinent tensile strength values obtained in 1020 steel.

No satisfactory joints were obtained in drill rod bars brazed with BAgMn brazing alloy.

D. DISCUSSION OF THE EXPERIMENTAL RESULTS

1. Characteristics and Significance of the Obtained Tensile Strength-Joint Thickness Curves

The four tensile strength-joint thickness curves of the four filler metals (pure Ag, pure Cu, BAg8 and BAgMn alloys) in mild steel all show the same pattern: the tensile strength of the joint increases with the decrease of the filler metal disk.

As has been mentioned previously according to the binary phase diagrams (Fe-Ag, Fe-Cu, Fe-Mn, Ag-Mn and Ag-Cu) the possibility of formation of intermetallic compounds in the investigated joints was completely excluded, the diffusion of the base metal into the filler metal in one case (Fe-Ag) was completely excluded and in the other three cases (Fe-Cu, Fe-BAg8 and Fe-BAg-Mn) it should be quite negligible, especially when one considers the extremely fast brazing time achieved by the induction heating method.

Consequently, the four curves represent four cases of the brazed joints where the increase of the tensile strength of the brazed joint is due exclusively, or almost exclusively, to the increase of the biaxial constraint; in other words, due to increase of suppression of the neck-down effect of the filler metal disk. Thus these four curves present an excellent opportunity for the study of the influence of the biaxial constraint upon the tensile strength of the filler metal under almost ideal conditions. For the exploratory stage of such studies the fact that the exact shape of these four curves is not

determined quite accurately is not so important, strictly speaking; however, the obtained curves are far from being perfect, as Figs. 20, 21, 22 and 23 show, for they were drawn by connecting three or four points of maximum tensile strength values with straight lines, and the extensions of these curves in the region of extremely thin and thick joints were obtained by extrapolation. This dies not imply that the exact shape of these curves cannot be determined. By further improving the induction heating method, and by obtaining a sufficiently high number of maximum tensile strength values for all possible joint thicknesses, the shape of these curves can be determined more accurately. Improvements of the induction brazing method have been considered and will be tried in the course of the future work on this problem. For example, it is planned to obtain extremely thick joints by casting the molten filler metal into the joint. Furthermore, experience has shown that thin joints of perfect quality can be achieved in the induction heating apparatus by filling the molten filler metal first in a joint of moderately small gap thickness, and then by squeezing the molten filler metal out of the joint by increasing the pressure on the ends of the base metal bars with the help of the screws, *C* (Fig. 7). Theoretically, there is no reason why extremely thin joints with a filler metal film only a few atoms thick cannot be achieved just by increasing the pressure on the ends of the bars shortly after filling the gap with molten brazing alloy; provided that the base metal surfaces are finished to the highest degree of precision, these surfaces are absolutely parallel to each other and the alignment of the bars is ideal. Practically, however, it is quite difficult to obtain extremely thin joints just by increasing the pressure. Usually, even by an almost ideal geometry of the joint, by increasing the pressure, the edges of the bars touch each other on one side of the joint, while on the opposite side there still exists a considerable gap. Thus the resulting brazed joint might be quite sound and voidless, the uniformity of the filler metal thickness, however, so unequal, that it would be impossible to determine the "thickness" of such a joint. This type of imperfection of brazed joints might be caused by an unequal thermal expansion in different sections of the tensile bars, due to a not quite uniform heating of the bars by the induction coils, or to the fact that it is extremely difficult to exert the pressure exactly along the axis of the base metal bars. If the end screws, *C*, do not exert the pressure axially, eccentric stresses are unavoidable, which must result in misalignment of the bars.

Considering the possibilities of achieving extremely high-tensile strength values in brazed joints with an excessively thin filler metal film, Case[1] points out that: "Extrapolation of tests made on brazed joints of varying filler film thickness leads to the belief that the strength of the joint should continue to increase as the film thickness is decreased, and a film only a few molecules thick should have maximum strength. In actual brazing operations, there is, however, an optimum film thickness below which the strength begins to taper. This is because in excessively thin films the quality of the bond between the brazing alloy and the base metal begins to deteriorate, due to the discontinuities of the film, broken by an increasing number of voids."

According to the experience made on brazing of tensile bars by the induction heating method, the decrease of the tensile strength in excessively thin joints was due, if not exclusively, then to a great part to the imperfections (voids, dewetting, etc.) of such extremely thin joints. Therefore, it can be anticipated that by further improving the brazing techniques as outlined above, the tensile strength of the excessively thin joints can be further increased.*

However, all these problems concerning the improvements of brazing techniques in order to obtain precise and continuous tensile strength-joint thickness curves are more important for future work. At the present stage of the research work, much more important is the fact that already from the obtained experimental results and from the achieved, though incomplete, tensile strength-joint thickness curves, a great deal of interesting information can be obtained and some important conclusions can be derived. An analysis of the obtained experimental results combined with an analysis of the experimental and theoretical work done by E. Orowan[10, 11] on the investigation of the influence of the biaxial constant upon the tensile strength of a metal gives a sound basis for development of a rather complete and comprehensive theory of the tensile strength of the brazed joints. Such an analysis is attempted in the following section.

2. *Analysis of the Factors Determining the Tensile Strength of the Brazed Joints. Increase of the Tensile Strength of a Metal Due to Biaxial Constraint*

For the fundamental understanding of the mechanism of fracture of a brazed joint the first most important question is: why and how can the tensile strength of a metal be increased by the biaxial constraint? This problem has been thoroughly investigated by E. Orowan and discussed in two papers, "Fracture and Strength of Solids"[10] and "Fundamentals of Brittle Behavior in Metals."[11]

Orowan has demonstrated in a very effective manner how the tensile strength of a metal can be increased by increasing the biaxial constraint by pulling tensile specimens of circular cross section provided with a circumferential notch as shown in Fig. 27. The notched section (with diameter *d*) is smaller than the rest (with diameter *D*), and so the tensile stress required for plastic deformation in an unnotched tensile specimen is reached here first. A small amount of axial plastic extension occurs; this, however, is accompanied

* The most recent progress made in the field of brazing of extremely thin and extremely thick brazed joints makes it possible to obtain fairly sound brazed joints in a wide range of joint thicknesses. By using the new techniques for brazing SAE 1020 steel and drill rod with pure silver, the minimum thickness obtained was 0.00003 in., the maximum, 0.25 in.

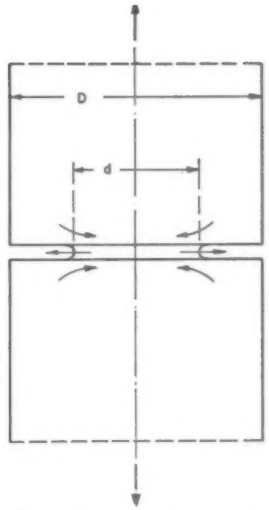


Fig. 27 Plastic constraint in a circumferentially notched tensile specimen (E. Orowan[11])

The curved arrows represent the shear stresses exerted by the contracting notched core upon the adjacent rigid bulks; the straight arrows indicate the transverse tension set up in the notched core. *d*, Diameter of the notched core; *D*, diameter of the full section.

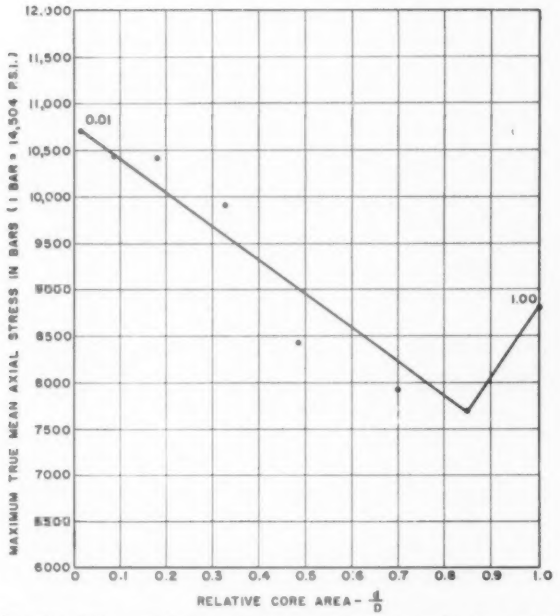


Fig. 28 The maximum true mean axial stress in the notch core of mild steel specimens with thin sawcut notches as a function of the ratio of the diameter of the core area to that of the unnotched cross section-relative core area, "d/D," according to Orowan, Nye and Cairns[12]

by a transverse contraction which the notch section cannot undergo freely because it is jointed to the full sections of the rod in which the yield stress has not yet been reached. As the notch core tries to contract, it exerts forces acting radially inward upon the full sections above and below; conversely, then these sections act with opposite forces (radially outward) upon the notch core and create transverse tensions in it. With an equal part of the axial tension, the transverse tensions form a hydrostatic tension and thus annihilate this part of the axial tension as far as its effect upon the plastic yielding of the notch core is concerned. As the axial tension is increased, more and more of it is automatically neutralized by transverse tensions in the notch core, so that the plastic deformation in the core cannot progess beyond the small amount required for producing the transverse tension necessary to stop further deformation. Considerable plastic deformation cannot start before the mean axial tension has increased to a certain critical magnitude, say, q times higher than the tensile yield stress, Y, of an unnotched rod. One can say that the notch core is under "plastic constraint": the number $q>1$ has been designated by Orowan as "mean constraint factor."

Orowan has proved that by increasing the diameter of the full section, D, and thus decreasing the ratio, d/D, the mean constraint factor, q, increases. In a series of experiments with circumferentially notched mild steel bars, Orowan, Nye and Cairns[12] have obtained true stress-strain curves for bars with various ratios of the notch core area to that of the unnotched cross section, d/D, or in other words, with various "relative core areas. The notch in their experiments was a thin saw cut (of 0.2 mm width) with a semicircular tip.

In Fig. 28, the maximum "true mean axial stress" values obtained in the narrowest part of the notch core by Orowan, Nye and Cairns are plotted against the "relative core area"—the ratio, d/D. As Fig. 28 shows, the maximum true stress has been increased from 8800 bars (127,600 psi) for an unnotched bar to 10,700 (155,150 psi) for a circumferentially notched bar with a "relative core area," $d/D = 1/100$. Thus the true tensile strength of the mild steel has been increased 10,700/8800 = 1.22 times and this increase was due only to the suppression of the neck-down effect. The next important question in the course of the analysis of the factors determining the tensile strength of the brazed joints is: how much can the tensile strength of a given metal be increased by a complete suppression of the neck-down effect, or, in other words, where is the upper limit of the tensile strength of a metal by increasing the biaxial constraint to the highest attainable value. This problem has also been thoroughly investigated by Orowan.[10, 11] He points out that the mean constraint factor of the ideally sharp and deep circumferential notch, in an ideally plastic material, is not infinity but only about 3.3. This can be recognized from the stress distribution in a plastic half-space indented by a frictionless flat cylindrical punch (Fig. 29a). This was calculated approximately by H. Hencky[13] in 1923; the result of this calculation was that the highest pressure, occurring in the center of the punch, is approximately 3.3 times the ordinary tensile or compressive yield stress of the indented material if this is assumed to be "ideally plastic"; i.e., nonhardening, with a sharp yield point. Now it is easily seen that the distribution of the tensile stresses around the core of an ideally sharp and deep notch (Fig. 29b) is the same as the distribution of the compressive stresses under the indenter in Fig. 29a. The stresses change only in sign if the

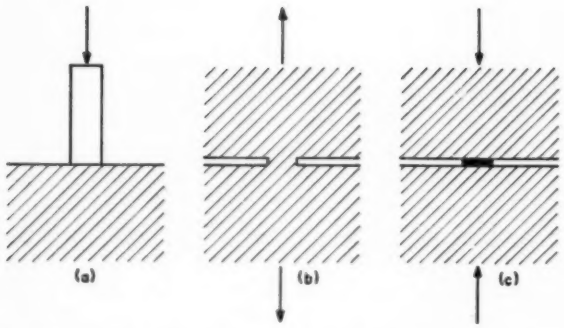


Fig. 29 Identity of stress field in a plastic half-space indented by a cylindrical punch (a) and (c) with that in a sharply notched, thick tensile specimen (b), according to Orowan[11]

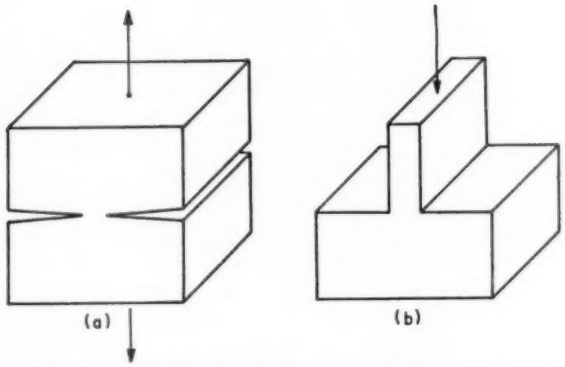


Fig. 30 Two-dimensional case corresponding to Fig. 29, according to Orowan[11]

notched specimen is axially compressed, instead of extended; since the notch core is infinitely short and since, for reasons of symmetry, there can be no shear stress in a cross section through the core, this can be replaced by a thin frictionless rigid circular disk (Fig. 29c). The stress and strain distribution above and below the disk, however, is that in an infinite half-space into which a flat-headed cylindrical punch is pressed. Hencky's result, therefore, shows that the maximum constraint factor of an ideally sharp and deep circumferential notch is not infinity but only about 3.3.

The two-dimensional case of infinitely long cracks running along a surface of a plate (Fig. 30a) is even simpler and can be treated mathematically exactly with the assumption of an ideally plastic material. The corresponding plastic indentation problem (Fig. 30b) was solved by Prandtl.[14] According to his result, the distribution of tensile stress in the notched cross section of a plate containing two opposite ideally sharp and deep notches is uniform, and the tensile stress in it is:

$$\left(1 + \frac{\pi}{2}\right) Y \cong 2.57Y,$$

where Y is the yield strength of the given metal. The mean and the maximum constraint factors, therefore, are here equal and their numerical value is 2.57.

The important conclusion from these theoretical considerations is that notch constraint cannot raise the tensile stress occurring during plastic yielding more than by a factor that is for an ideally plastic material between 2.5 and 3.3.

However, these maximum values, determined by Hencky and Prandtl, and Orowan are not applicable in the case of brazed joints since there is a great difference between the mechanism of fracture in a brazed joint. in the theoretical model employed by Orowan (Fig. 29b), the plastic materials of the two half-spaces are identical. In the case of the brazed joints, however, the material of the filler metal and that of the bars are quite different.

In order to adjust Orowan's model for the case of brazed joints, some changes have to be made. Let us consider first the case where in Orowan's model the material of the two half-spaces is a rigid steel and the material of the infinitely thin disk between these two half-spaces (Fig. 29) is a brazing alloy with a comparatively low yield strength. In such a model much higher maximum mean constraint factors, and, consequently, much higher maximum yield stress values can be anticipated than in circumferentially notched bars of brazing alloy, provided that the brazing alloy disk in the given model is completely voidless and the metallic bond on the filler metal-steel interface is absolutely sound. This reasoning has been fully confirmed by the experimental results obtained on brazing drill rod bars with pure silver, pure copper and BAg8 alloy.

Especially impressive are the experimental results obtained on pure silver joints. The yield strength of pure silver, according to the *Metals Handbook*,[8] is equal to 7900 psi. According to Orowan, in a circumferentially notched silver bar with an infinitely small relative core area, d/D, the maximum yield stress is approaching the upper limit, which cannot be larger than 3.3 times the yield stress Y in uniaxial tension.

In silver brazed drill rod bars, however, the yield strength of pure silver—which under the specific conditions of fracture in very thin brazed joints was almost equal to the ultimate tensile strength of the joint—has been raised to 84,350 psi (ADR 116, Table 6; Fig. 25). That means an increase of yield strength of silver

$$\frac{84{,}350}{7900} = 10.7 \text{ times.}$$

Speaking in terms of Orowan's theory this indicates that the maximum mean constraint factor in this particular silver joint was in the order of magnitude of about 11.

Consequently, the yield strength of a metal in a brazed joint under certain conditions can be increased much higher than 3.3 times. This conclusion can also be derived from the experimental results obtained by Zschokke and Montandon[15] and also by Orowan, Nye and Cairns.[12] In a test made by the latter authors a disk of tin, 0.4 mm thick and of $^1/_2$ in. diam, was welded by pouring tin between the flat ends of mild steel bars; tensile failure occurred by separation at the boundary

layer between the two metals at a mean axial tension of 1250 bars (18,125 psi) while the ultimate stress of the cast pure tin used was 225 bars (3265 psi). That means an increase of the ultimate tensile strength of tin 1250/250 = 5 times.

The maximum increase of the ultimate tensile strength of silver in brazed joints achieved in the experiments performed at the Armour Research Foundation was 84,350/21,000 = 4 times.

E. CONCLUSIONS

In summary the following conclusions are evident:

1. The four tensile strength-joint thickness curves of low-carbon steel bars brazed with pure silver, pure copper, BAg-8 and BAgMn filler metals (Figs. 20, 21, 22 and 23) represent four cases of brazed joints, where the increase of the tensile strength of the joints by decreasing the joint thickness is due exclusively, or, almost exclusively to the increase of the biaxial constraint, or, in other words, due to the suppression of the neck-down effect of the filler metal disk.

Due to the almost complete suppression of the plastic deformation in the extremely thin joints, the stress distribution in the extremely thin filler metal disks might approach a state of very high triaxiality, or, in more simple words, a state of almost ideal hydrostatic tension. Under such conditions, the fracture strength of pure silver, which in a standard tensile test is equal to 21,000 psi, can be increased, as the pertinent experimental results show, to 84,350 psi (sample ADR 116). The fracture strength of pure copper, which in a standard tensile test is equal to 32,000 psi, can be increased to 106,500 psi (sample CDR 249).

2. The tensile strength-joint thickness curves of pure silver, and pure copper joints in SAE 1020 steel and drill rod bars (Figs. 25 and 26) show quite evidently that the tensile strength of the brazed joints increases with the increase of the tensile strength of the base metal: in all joint thickness ranges the drill rod curves show much higher tensile strength values than the SAE 1020 steel curves. Consequently, it can be anticipated that by further increase of the strength of the base metal the fracture strength of pure silver, or of pure copper can be elevated in extremely thin joints into the region of brittle fracture of these metals. Speaking in terms of practical brazing, that means that the brittle fracture strength of a given pure filler metal represents the upper limit for the tensile strength of this filler metal which can be achieved in extremely thin joints under ideal conditions of a perfect, or almost perfect, triaxiality of stresses.

3. Since all of the investigated silver brazed joints, even the joints with the highest tensile strength, broke in the middle of the silver disk, so that the plane of fracture was identical with the plane of symmetry, and since the fracture never occurred along the steel-silver interface, it is quite obvious that the steel-silver interface was always stronger than the silver itself, even under a tensile strength of 84,350 psi.

This conclusion is quite important, since until now in the theory of brazing it was often assumed that for obtaining strong brazed joints the formation of an alloy system between the filler metal and the base metal is an essential prerequisite. Since silver and iron are absolutely insoluble in each other, and, consequently, the iron-silver interface must be considered as a classic example of an "adhesion bond," it is quite obvious that for achieving sound and strong brazed joints the alloying between the filler metal and base metal is not always necessary.

F. ACKNOWLEDGMENTS

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Local Buckling of Intermittently Welded Structural Members*

Test results lead to the recommendation that consideration be given to incorporating critical gap-width ratio ideas into clauses which control the permissible gap in intermittent welding

by C. H. Norris and J. B. Scalzi

Synopsis

This is the second and final paper dealing with a research project which was conducted in the Structural Analysis Laboratory of the Massachusetts Institute of Technology under the sponsorship of the Structural Steel Committee of the Welding Research Council. The objective of this program was to provide a basis for substantiating or revising current specifications governing the use of intermittent fillet welds to connect the component parts of structural members

When it is unnecessary to use continuous welds in order to satisfy the strength requirements of a connection, intermittent welds may be used. However, the clear space between such welds must be short enough to preserve essentially all of the local buckling resistance which the component parts of the member would possess if the welds were continuous. In many cases, the determination of the local buckling characteristics of welded struts and flexural members reduces essentially to the study of the buckling of long rectangular plates compressed on the ends and supported on the long edges in various manners.

Tests and a theory for such plates were reported in the first paper and continued as part of the last half of the program. These tests led to the conclusion that there is a gap, defined by a critical gap-width ratio, below which an intermittently supported plate retains essentially all the buckling resistance of a continuously supported one. Based on these ideas, twelve columns and six beams were designed and tested. There actual performance was compared with that predicted from the plate tests.

It was concluded that local buckling of the elements of structural members followed the same principles as were found for long rectangular plates. However, members with intermittent welds cannot take the abuse of those with continuous welds and will buckle locally if the stress exceeds the yield point.

The recommendation is made that consideration be given to incorporating the critical gap-width ratio ideas into clauses which control the permissible gap. Further it is recommended that intermittent welding be restricted to secondary members, which are subjected to static rather than dynamic loading conditions. It must be borne in mind that intermittent welding introduces stress raisers which may be dangerous, from a fatigue viewpoint, if the loading condition is other than static.

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INTRODUCTION

IN RECENT years, there has been a growing interest in the evaluation of ultimate strengths of various types of structures. If the full capacity of a structural material is to be utilized so as to achieve the maximum possible ultimate strength in a structure, it is of course necessary that there be no premature buckling of the structure as a whole, or of any of its members. Local buckling of the component parts of these members may also decrease the ultimate strength of a structure. For example, local buckling of a cover or web plate of a compression member, or of a flange plate of a flexural member, may cause premature buckling of the member as a whole. On the other hand, local buckling of the web plate of a beam would not seriously affect its ultimate load-carrying capacity if the vertical stiffeners were properly designed.

The designer should always realize, therefore, that local buckling of a component part may lower the efficiency of a member as a whole, and invite premature failure of an otherwise adequate structural member. In such cases, he should always strive to provide as much local buckling strength in the component parts as integral buckling strength in the member as a whole.

The integral buckling resistance of a compression or flexural member depends upon its bending and torsional stiffness. The local buckling resistance of a component part of such a member depends, however, upon the width-thickness ratio of the part and the manner in which its edges are supported.

The designer provides strength to resist local buckling by conforming with requirements such as the following which have been extracted from typical specifications for carbon steel members:

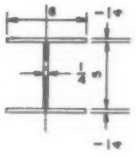
1. In compression members, the unsupported width, b, of a web plate must not exceed 32 times its thickness t;
2. The clear space or gap, g, between intermittent fillet welds must not exceed 16 times the thickness of the thinner plate joined.

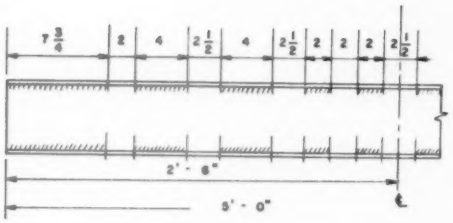
Many feel that specifications such as these dealing with the local buckling of structural members involve problems which require further study and research. For example, there appears to be no experimental or theoretical evidence to substantiate current design specifications governing the use of intermittent fillet welds to connect the component parts of structural members. The project, the completion of which is reported herein, was therefore set up to study this particular aspect of local buckling.

FORMULATION OF THE PROGRAM

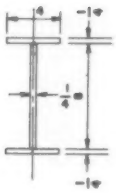
Consider several typical compression members having cross sections such as shown in Fig. 1. If such compression members are axially loaded without eccentricity, elements such as the web of column C-1 and the flange plate of column C-9 act as long rectangular plates loaded in their plane by uniform compressive forces on the two ends and supported in some manner along the two long edges. Elements such as the out-

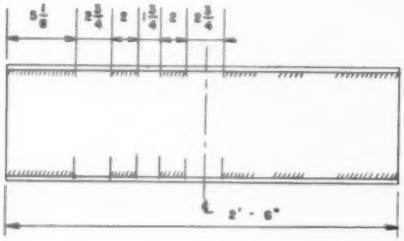
COLUMN
No. C-3 and C-4
(C-1 and C-2 similar
except no gaps)



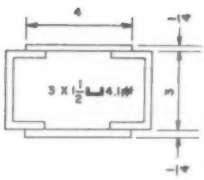


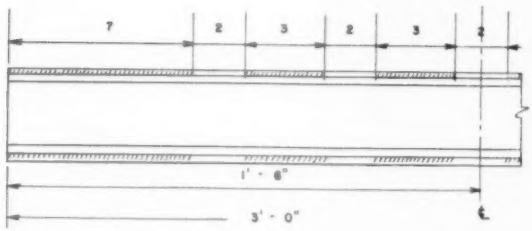
COLUMN
No. C-7 and C-8
(C-5 and C-6 similar
except no gaps)



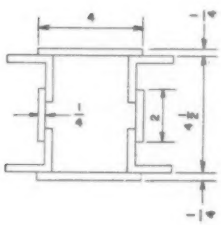


COLUMN
No. C-10
(C-9 similar
except no gaps)





COLUMN
No. C-12
(C-11 similar
except no gaps)



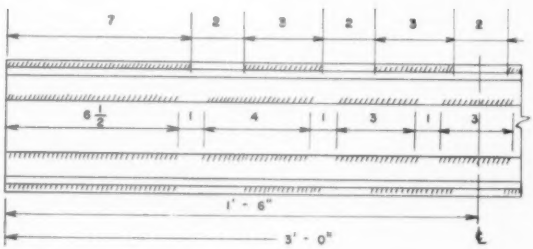
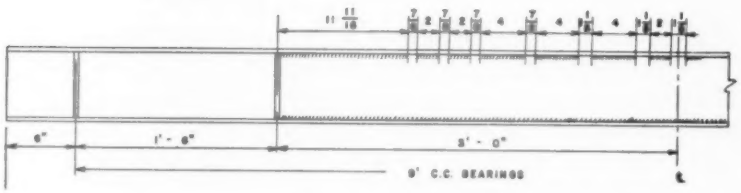
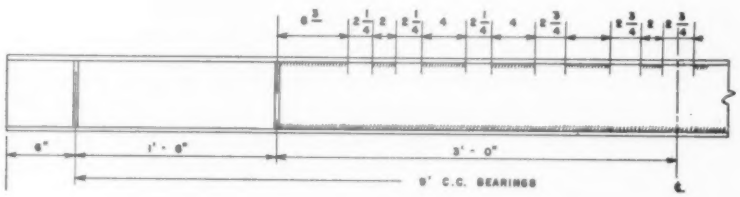


Figure 1

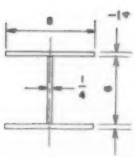
BEAM
No. A-1



BEAM
No. A-1-B



BEAM
No. A-2



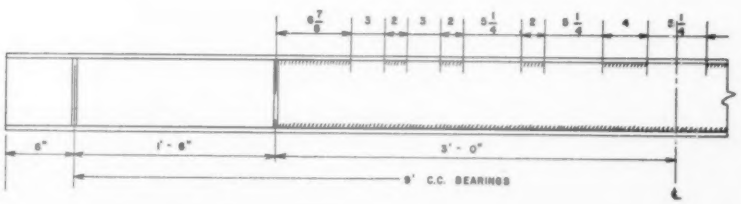


Figure 2

standing flanges of column C-1 are also essentially long compressed rectangular plates but in this case supported on only one long edge and free on the other.

Of course, if compression members are eccentrically loaded, the plate elements may be subjected to not only nonuniform end forces, but also shearing forces along the long edges—though the latter forces are usually small. The flange of a flexural member such as beam A-1 in Fig. 2 would be subjected not only to possible end compression but also to shearing forces through the weld connecting it to the web. The width of the outstanding flange is a small fraction of the length of a flexural member, and it is also often true that the shearing forces are small in the region of maximum flange compression. It is, therefore, usually possible to isolate a relatively long portion of such a flange plate, and then, neglecting the small shearing forces along the connection to the web, to consider this portion subjected only to end compressions.

Local buckling in a flexural or compression member may take place by an isolated buckling, in one of these web or flange plate elements, which does not involve flexural or torsional buckling of the member as a whole. Many of the most important local buckling situations of such elements may be studied by considering the buckling behavior of long rectangular plates compressed on the ends and supported on the long edges in various manners.

In many cases of built-up welded structural members, the various elements are connected together by continuous welds. When it is unnecessary to use continuous welds in order to satisfy the strength requirements of the connection, intermittent welds may be used. From a buckling standpoint, however, the clear space (or gap) between such welds must be short enough to preserve essentially all of the local buckling resistance which the component parts of the member would possess if the welds were continuous. Since the objective of this program is to investigate the effect of intermittent welds on the local buckling characteristics of a member, the study apparently resolves essentially into a comparison of the buckling behavior of intermittently and continuously supported rectangular plates.

Of course, a complete investigation of the local buckling phenomena in intermittently welded structural members would involve consideration of a very large number of cases. It was hoped, however, that a limited program would lead to conclusive and useful results. The investigation was, therefore, formulated as follows:

1. A theoretical and experimental

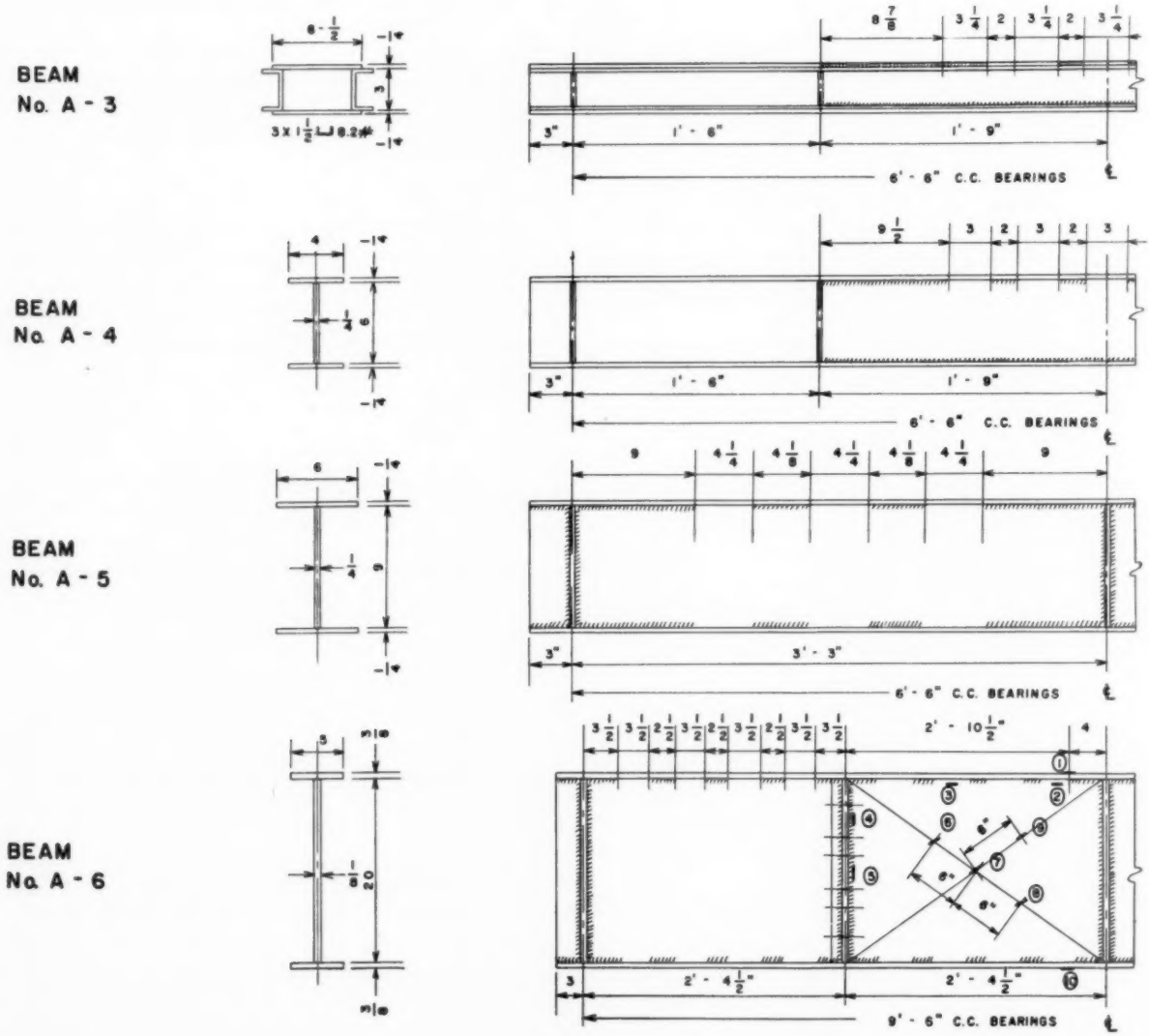


Figure 3

study was conducted of the buckling behavior of some of the simpler cases of long rectangular plates compressed on the ends and intermittently supported on the long edges. The results of this study were used to develop a method of estimating the local buckling behavior of more complex cases.

2. Typical structural members involving both continuous and intermittent welds were designed and tested. Their behavior was compared with each other and with that predicted by the results of step (1). The scope of these tests was broad enough to justify certain general conclusions concerning the effect of intermittent welds on local buckling behavior.

The first step of this program involved studying the buckling behavior of seven different series of long rectangular plates, namely: Series A, B, C, D, E, F and G. The type of compressive load on the short ends and the type of support on the long edges were varied in these seven series as described in Appendix A of this paper.

Series A, B, C and D were described in an earlier paper.[1] Series E, F and G are discussed in Appendix A herein. The results of all seven series are summarized in the following section. The tests of the typical structural members involved in Step (2) of this program are reported on below in subsequent sections.

BUCKLING OF INTERMITTENTLY SUPPORTED RECTANGULAR PLATES

In the earlier paper,[1] a theory was developed for the buckling behavior of intermittently supported rectangular plates, and described in detail for the Series A type plate—that is, a long rectangular plate simply supported at intermittent points on both long edges and acted upon by a uniformly distributed load on the short ends. The extension of this theory to the Series B, C and D cases was also described briefly in the first paper. This treatment has been summarized herein in Appendix A and further extended to cover the cases involved in Series E, F and G.

Again it should be emphasized that the theory has been specifically derived for certain idealized situations which are approached but not completely realized in the actual flange and web plate elements found in actual structural members. The theory has been derived for relatively long rectangular plates which are loaded in their plane by compressive forces on the short ends and intermittently supported along the two long edges. In some cases the end compressions are uniformly distributed, and in some cases vary in a triangular manner. In all cases the short ends are considered to be *simply supported*—i.e., displacement normal to the plane of the plate is prevented, but the end is free to rotate and move parallel to the plane of the plate. The long edges, however, are not loaded in the plane of the

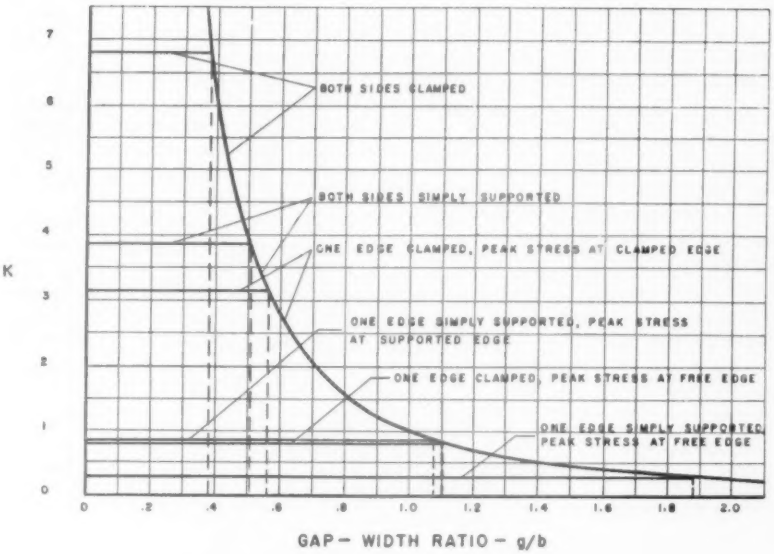

BOTH SIDES CLAMPED
BOTH SIDES SIMPLY SUPPORTED
ONE EDGE CLAMPED, PEAK STRESS AT CLAMPED EDGE
ONE EDGE SIMPLY SUPPORTED, PEAK STRESS AT SUPPORTED EDGE
ONE EDGE CLAMPED, PEAK STRESS AT FREE EDGE
ONE EDGE SIMPLY SUPPORTED, PEAK STRESS AT FREE EDGE
K
GAP – WIDTH RATIO – g/b

Fig. 4 For triangular end compression

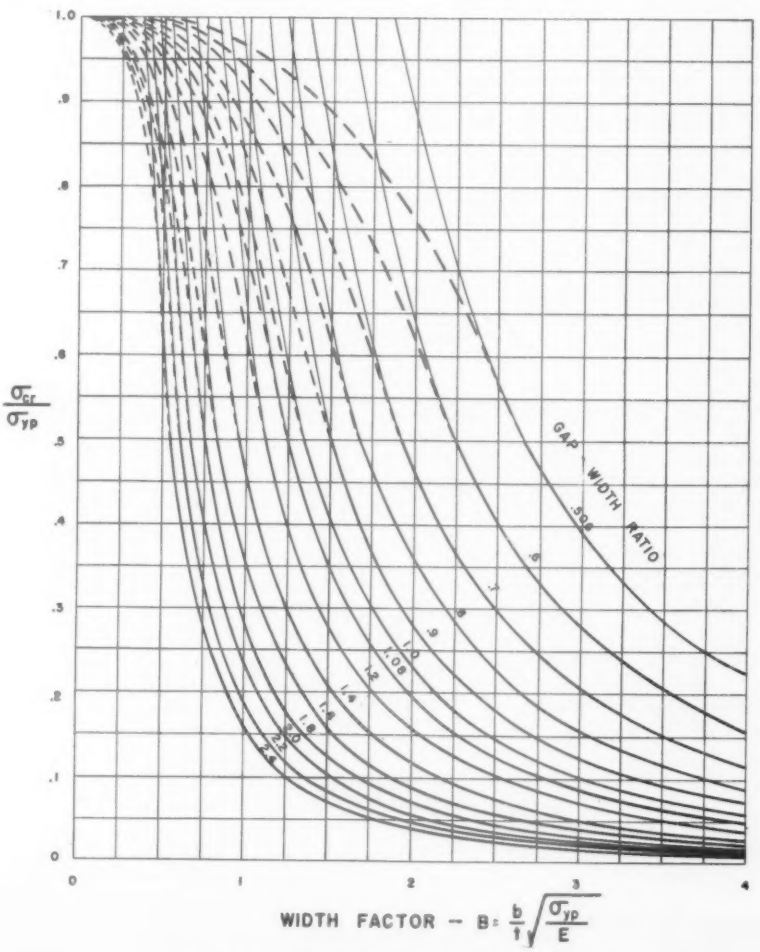


Fig. 5 For triangular end compression

plate and are considered, depending on the case involved, to be either (1) free, or (2) simply supported at points which are uniformly spaced along the edges, or (3) clamped at points which are uniformly spaced along the edges. The clear spacing between these intermittent point supports is called the gap, g.

The same assumptions are made with regard to the material, initial curvature, eccentricities and plate bending in the theory for intermittently supported plates as were made in the development of the classical Bryan formula for continuously supported plates. See eq (a) in Appendix A for the generalized form of the Bryan formula.

The application of the theory to the seven specific cases of intermittently supported plates leads to the following conclusion which is likewise definitely substantiated by the extensive tests of Series A to G, inclusive:

For any specific case of a long rectangular plate compressed on the short ends and intermittently supported on the long edges, there is some spacing of the intermittent supports, defined by a so-called "critical gap-width ratio," below which the plate retains essentially all the buckling resistance of a similar continuously supported plate.

This conclusion has been so clearly demonstrated by theory and tests for the seven cases specifically considered, that it appears legitimate to extend the application of this theory to other cases which were not specifically considered. This was done in considering the design of the column and beam test specimens involved in Step (2) of this program.

For intermittently supported plates, the average intensity of the end compression at which buckling occurs (also equal to the average compressive stress caused by the buckling load and, therefore, called the "critical stress") may be computed by inserting the proper value of K into eq (a) of Appendix A, the generalized form of the Bryan formula. The value of K depends upon the type of end compression and the type and spacing of the intermittent support on the long edges. The proper value of K may be obtained from a diagram such as Fig. 4 where K is plotted against the gap-width ratio of the plate. Such a diagram is constructed by first plotting a curve the ordinates of which are $(b/g)^2$. Then horizontal lines are drawn corresponding to the value of K for a continuously supported plate of the type being considered. For any particular type of intermittently supported plate, the intersection of the horizontal line corresponding to the K of a continuously supported plate of that type with the $(b/g)^2$ curve defines the "critical gap-width ratio" for that particular type plate. For gaps corresponding to values lower than this critical ratio, the value of K is the same as if there were no gap (i.e., the plate buckles as if it were continuously supported on the long edges). For gaps corresponding to gap-width ratios above the critical value, the value of K is given by the ordinate of the $(b/g)^2$ curve [i.e., the plate buckles in the gap as an end-supported column for which Youngs' modulus E is replaced by $E/(1-\nu^2)$]. The application of the diagram to six different cases is noted on Fig. 4.

From the standpoint of writing a specification to insure adequate resistance to local buckling in the gap of an intermittently welded member, we are not interested so much in the exact critical stress for a particular case but rather in whether the critical stress is essentially the same as if the gap were zero. In other words, we wish to be sure that the critical gap-width ratio is not exceeded. In reaching final conclusions later, we should keep in mind that both theory and tests demonstrate that buckling resistance does not decrease rapidly for thick plates (i.e., plates having a low width-thickness ratio) even when the critical gap-width ratio is exceeded. On the other hand, as the plate gets thinner and thinner (i.e., the width-thickness ratio increases), the loss in buckling re-

Table 1—Summary of Critical g/b Ratios

Type of end compression and support on long edges	*K*	*g/b*
Uniform end compression		
Both edges simply supported	4.0	0.50
One Edge Simply Supported	0.50	1.4
Uniform end compression		
Both edges clamped	7.0	0.40
One edge clamped	1.3	0.90
Triangular end compression		
Both edges simply supported	3.9	0.51
One edge simply supported, peak load at supported edge	0.85	1.1
One edge simply supported, peak load at free edge	0.29	1.9
Triangular end compression		
Both edges clamped	6.8	0.38
One edge clamped, peak load at clamped edge	3.2	0.56
One edge clamped, peak load at free edge	0.82	1.1

sistance takes place more and more sharply as the critical gap-width ratio is exceeded.

In Table 1, the critical gap-width ratio is noted for a number of different cases of intermittently supported rectangular plates. Likewise noted are the values of K for corresponding cases of continuously supported plates.

TEST PROGRAM FOR INTERMITTENTLY WELDED STRUCTURAL MEMBERS

The cross sections of the test beams and columns were selected as being typical of actual structural members, although in some cases it was necessary to use rather unusual proportions in order to investigate certain characteristics. Standard welding techniques were used in fabricating the members. As a result we attempted to use typical plate thicknesses although these tended to be on the thin side to reduce costs, handling difficulties and loading requirements.

The cross-sectional dimensions of the test members were selected so as to make web plates the critical elements in certain cases, and outstanding flanges the critical elements in other cases. The term "critical element" means the element which has a gap-width ratio equal to or in excess of its critical value. Consider, for example, column C-3, Fig. 1, which has a 5-in. web and 3-in. outstanding flanges. Using the critical gap-width ratios from Table 1, the critical gaps for the web or flange depending on whether the intermittent supports are considered to be simple or clamped are as follows:

Web:
Simple, $g = 0.50 \times 5.0 = 2.50$ in.
Clamped, $g = 0.40 \times 5.0 = 2.00$ in.
Flange:
Simple, $g = 1.40 \times 3.0 = 4.20$ in.
Clamped, $g = 0.90 \times 3.0 = 2.70$ in.

The actual gap being 2.50 in. in this case, the web becomes a so-called critical element.

The test specimens in this program were subdivided into three groups:

1. Columns with intermittent welds over the entire length. All columns belong to this group.
2. Beams with intermittent welds in the central portion of the length, which portion was subjected to pure bending. Beams A-1, A-2, A-3 and A-4 are included in this group.
3. Beams with intermittent welds over the entire length and subjected to mid-span loading so that welds were acted upon by the combined effect of shear and bending. Beams A-5 and A-6 belong to this group.

Pertinent information concerning the test specimens is summarized in Table 2. Detailed dimensions of the column specimens are shown in Fig. 1, and of the beam specimens in Figs. 2 and 3.

Table 2—Summary of Column and Beam Test Specimens

Group	*Specimen No.*	*Critical element*	*Gap length, in.*	*Special remarks*
Columns	C-1	..	0	Slenderness ratio of these columns varied from 26 to 41. Ends of columns milled. Tested with spherical seats. Ultimate load considered to be that load at which a local buckle first appeared.
	C-2	..	0	
	C-3	Web	2.5	
	C-4	Web	2.5	
	C-5	..	0	
	C-6	..	0	
	C-7	Flange	2.75	
	C-8	Flange	2.75	
	C-9	..	0	
	C-10	Flange	2.0	
	C-11	..	0	
	C-12	Web	1.0	
		Flange	2.0	
Beams (bending only)	A-1	Web	1.25	Beams loaded with equal loads at quarter points. End quarters continuously welded. Ultimate load considered smaller of yield load and load at which local buckle appeared.
	A-1-B	Web	2.75	
	A-2	Web	5.25	
	A-2-W	..	0	
	A-3	Flange	3.25	
	A-4	Flange	3	
Beams (shear and bending)	A-5	Flange	4.25	Beams loaded at midspan. Intermittent welds throughout. Same definition of ultimate load as for other groups of beams.
	A-6	Flange	3.5	

NOTE: For plate stock used in fabricating these specimens—yield point stress in compression = 38,400 psi; yield point stress in tension = 37,700 psi; ultimate stress in tension = 59,800 psi; modulus of elasticity in compression = 30,100,000 psi; modulus of elasticity in tension = 29,400,000 psi.

Note that in most all of these specimens the critical gap is less than 16 times the plate thickness. Note further that most all of these plates are relatively thick, i.e., have low width-thickness ratios. In such cases, the plate tests discussed above indicated that local buckling resistance did not decrease rapidly above the critical gap-width ratio. Originally, therefore, it was planned to make more test specimens having gaps upward to 16 times the plate thickness. After testing the above specimens, however, the effect of intermittent welds was considered to be sufficient so that additional tests at greater gaps were deemed unnecessary.

DESCRIPTION OF COLUMN TESTS

After fabrication, the ends of all column specimens were milled. From four to six SR-4 gages were mounted near mid-length of the column. The columns were tested in a 300,000-lb Southwark Emery hydraulic testing machine, which was equipped with spherical seats. (See Fig. 11.) After centering the specimen as accurately as possible on the seats and mounting a dial gage to measure the lateral deflection at mid-height, the column was loaded up to its ultimate load by a series of increments. Of course, after each increment of load, the strains and lateral deflection were measured and recorded. The ultimate load in all cases was the load at which the first local buckle appeared.

RESULTS OF COLUMN TESTS

The results of column tests are summarized in Table 3. The condition of the column specimens after testing are shown in Figs. 12, 13 and 14 of Appendix B.

Table 3—Summary of Column Tests

Column No.	*Gap length, in.*	*Ultimate load, lb*	*Area, sq in.*	*Average stress, ksi*
C-1	0	167,000	4.16	40.2
C-2	0	169,500	4.14	40.9
C-3	$2^1/_2$	159,000	4.14	38.4
C-4	$2^1/_2$	158,500	4.16	38.2
C-5	0	155,500	3.92	39.7
C-6	0	142,000*	3.89	36.5
C-7	$2^3/_4$	145,500	3.89	37.4
C-8	$2^3/_4$	149,400	3.92	38.2
C-9	0	193,500	4.40	44.0
C-10	2	180,000	4.44	40.5
C-11	0	327,000	6.84	47.8
C-12	2 and 1	289,000	6.82	42.4

* = Eccentricity was evident.

It is significant that, in all but one case —column C-6—the columns with continuous welds sustained higher ultimate loads than those with intermittent welds. Note also that, with the exception of this one case, the average stress at ultimate load for the columns with continuous welds exceeded the compressive yield point of the plate material. It is likewise significant that the average stress at ultimate load for the columns with intermittent welds was equal to or exceeded slightly the compression yield point of the material.

It is also of interest to note that the sections which were built up out of a combination of plates and rolled shapes—namely columns C-9 to C-12, inclusive—were capable of carrying higher stresses and of standing more abuse than those which were made up simply out of a combination of plates. Apparently in columns of the latter type—namely, columns C-1 to C-8, inclusive—incipient buckling in the critical plate element may throw enough load onto an adjacent element to cause it to likewise buckle.

DESCRIPTION OF BEAM TESTS

All of the beams were tested in a 100,000-lb Riehle Testing Machine. A typical setup is shown in Fig. 15. In cases where lateral instability was a factor, guides were provided to prevent this by blocking out from the standards of the testing machine. Two SR-4 gages were mounted on each flange, and vertical displacement was measured at midspan with a dial gage. Lateral displacement was likewise measured on the top and bottom flange to detect incipient lateral buckling of the beam.

The load was applied in increments up to the ultimate load, strain and dial gages being read after each load increment. Ultimate load was considered to be either the load at which local failure occurred in a gap, or the load at which the beam deflected excessively upon a slight increase in load.

In the case of beams A-1, A-3 and A-4, note that intermittent welds were used on only one flange. In such cases, the beam was tested first to ultimate load with the intermittent welds on the compression flange. The beam was then inverted and loaded to ultimate load with the continuous weld on the compression flange. In the case of beam A-2, both flanges were first tested with intermittent welds. After these tests, the gaps were filled in on both flanges, and the member with continuous welds retested both direct and inverted.

RESULTS OF BEAM TESTS

The results of the beam tests are summarized in Table 4. The condition of the

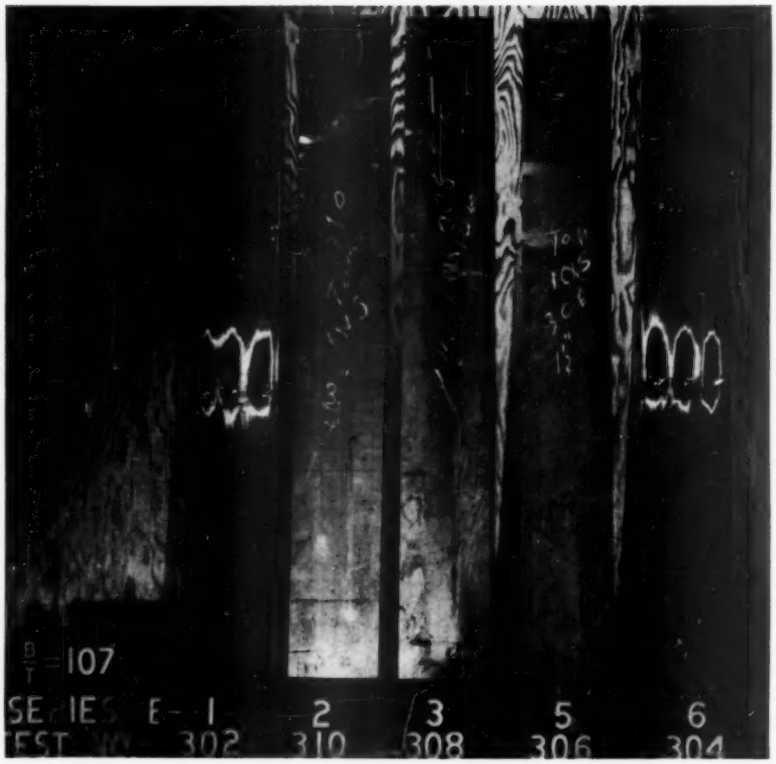


Figure 6

Figure 7

Table 4—Summary of Beam Tests

1 Beam No.	2 Critical Element	3 Gap length, in.	4 Load, lb, max
A-1	Web	0	43,100*
A-1 R†	Web	1 1/8	49,220
A-1-B	Web	2 3/4	48,400
A-1-B *R*	Web	0	48,800
A-2	Web	5 1/4	30,800
A-2 *R*†	Web	5 1/4	29,600
A-2 *W*‡	Web	0	29,940
A-2 *W R*†	Web	0	29,180
A-3	Cover plate	3 1/4	37,900
A-3 *R*†	Cover plate	0	38,400
A-4	Flange	3	41,440§
A-4 *R*†‡	Flange	0	42,100
A-5	Flange	4 1/4	47,600
A-5 *R*†	Flange	4 1/4	47,060§
A-6	Flange	3 1/2	52,700

* Lateral buckling set in.
† *R* = Beam inverted.
‡ *W* = gaps welded.
§ Buckled in gap.

beam specimens after testing are shown in Figs. 16, 17 and 18.

These specimens, whether continuously or intermittently welded, were designed to sustain yield stress intensities without undergoing local buckling. Note that in only two cases—namely beams A-4 and A-5R—did local buckling occur before the yield load was reached. In both of these cases, local buckling occurs in the gap of an intermittent weld. It is significant, however, that in all cases—even including these two—the ultimate load is essentially the same whether the compression flange has intermittent or continuous welds.

Beams A-5 and A-6 were designed to see if the predicted and observed behavior were consistent even for cases where the intermittent welds are transmitting sizable shearing stresses. Recall that the theory for the buckling behavior of intermittently supported plates was built up on the assumption that the plate was subjected simply to end compressions and no forces were applied to the plate in its plane along the long edges. In both of the test beams, however, the behavior predicted on the basis of such a theory coincided very well with the observed performance. For example, beam A-5 did not buckle locally although, when inverted and retested, it did buckle in the gap but at essentially the yield load.

The web of beam A-6 had a depth-thickness ratio of 160 and was designed to buckle under diagonal compression prior to reaching yield stress intensities in the flange. Even under such extreme conditions, buckling occurred in the flange or web at the gaps.

CONCLUSIONS AND RECOMMENDATIONS

It should be emphasized that the following conclusions and recommendations are based purely on the effect of intermittent welds on the local buckling characteristics of *statically loaded members*. No consideration has been given in this paper to the effect of intermittent welds on the fatigue properties of a member. It must be borne in mind that intermittent welding introduces stress raisers which may be dangerous, from a fatigue viewpoint, if the loading condition is other than static.

The results of this research demonstrate that the critical stress at which local buckling occurs in compressed plate elements of intermittently welded structural members may be computed using the Bryan formula, eq (a) in Appendix A. The value of K to be inserted in this formula

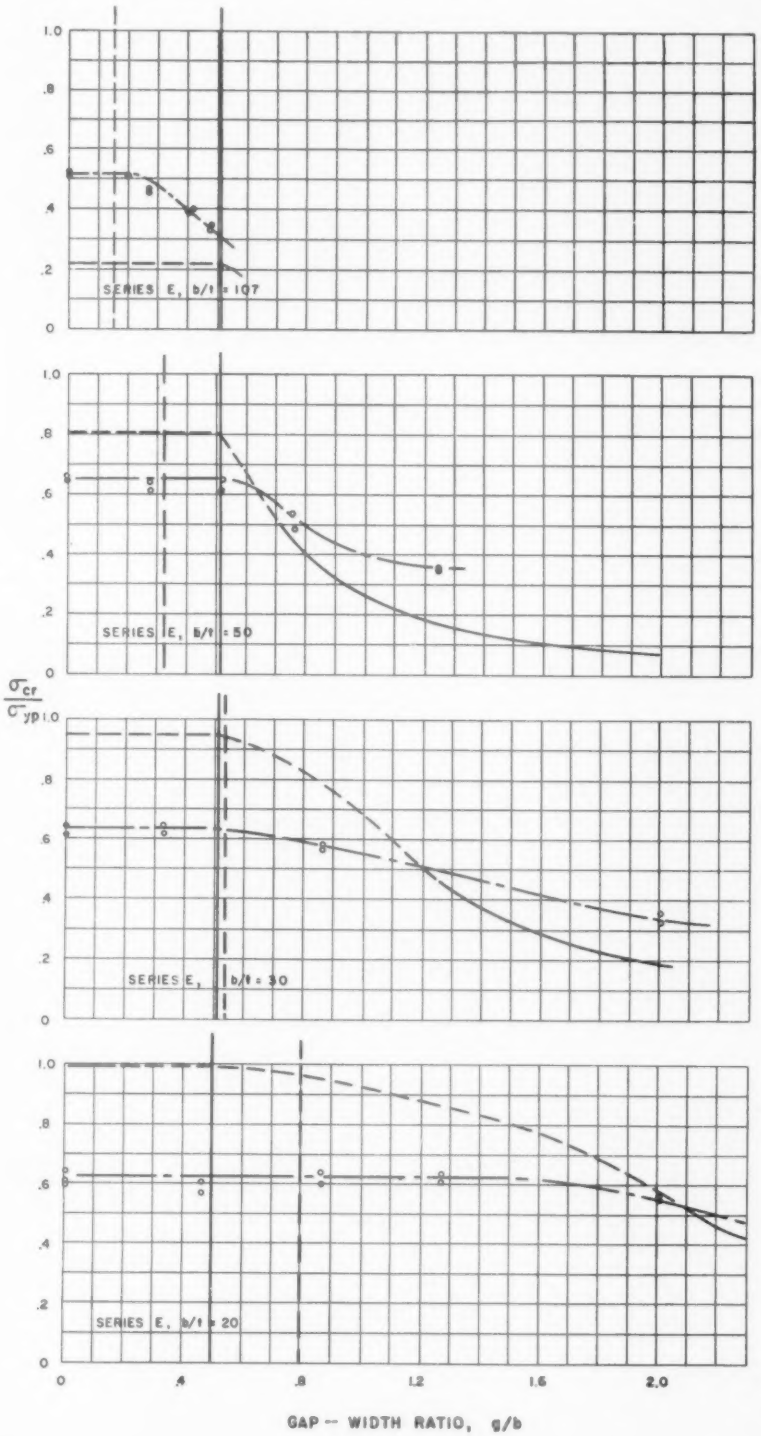


Figure 8

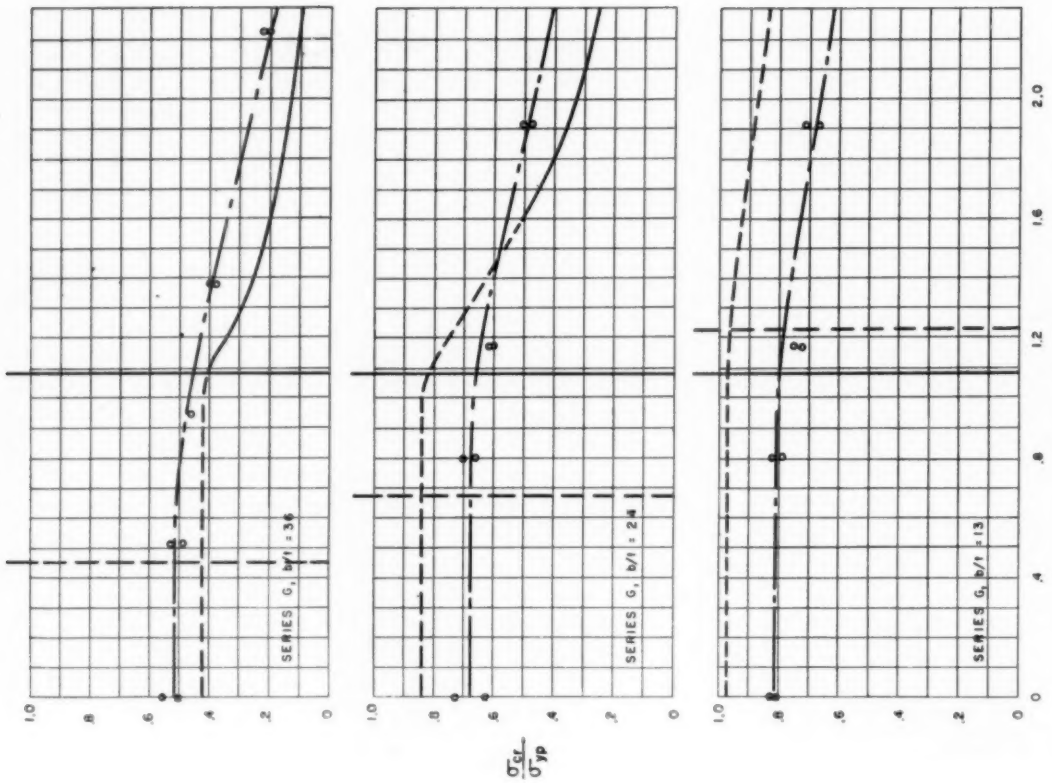


Figure 10

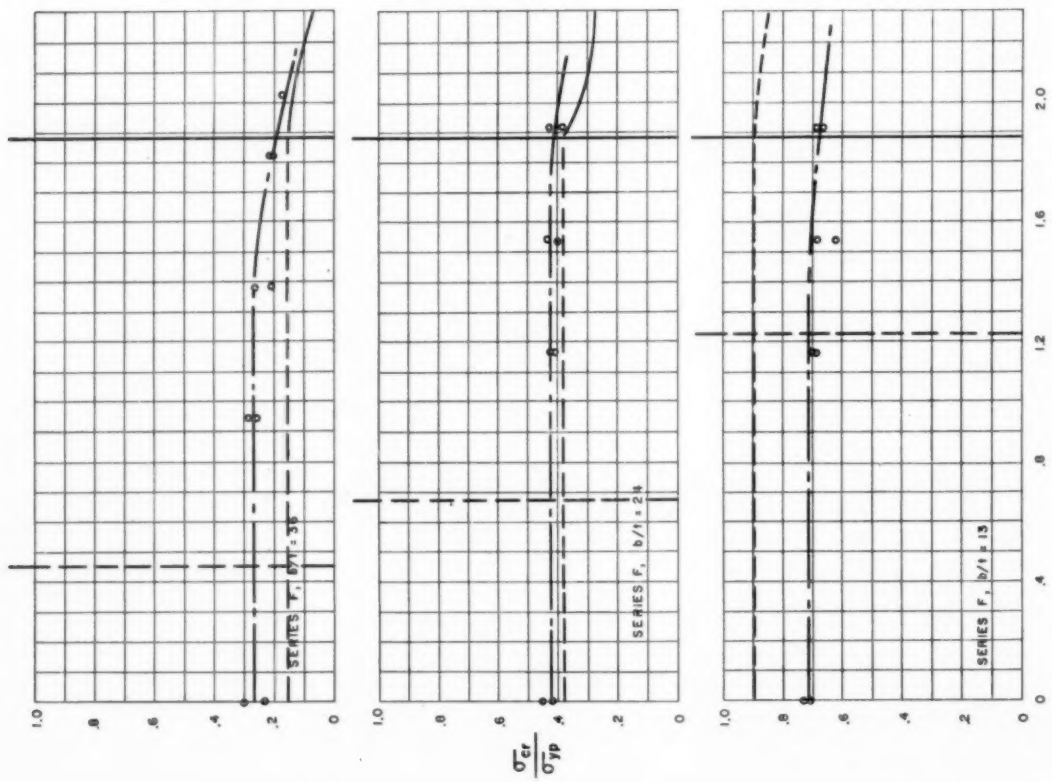


Figure 9

depends upon the type of support along the long edges of the plate element, upon the type of compression on its short ends, and upon the ratio of the gap between the intermittent welds to the width of the plate element.

For any particular type of intermittent edge support, there is a critical gap-width ratio below which the plate element has all the buckling resistance of an element with a continuously welded edge, but above which the buckling resistance varies inversely with the square of the gap-width ratio. In any particular case, the critical gap-width ratio may be obtained, as shown in Fig. 4, from the intersection of the horizontal line, representing the K of a similar continuously supported plate, with the $(b/g)^2$ curve. For gap-width ratios below this critical value, the critical stress may be obtained from eq (a) using the value of K of a continuously supported plate; for ratios above the critical value, however, K has a value equal to $(b/g)^2$.

On the basis of the theoretical and experimental results of this program, the following conclusions appear to be justified:

1. If the critical gap-width ratios of the various plate elements are not exceeded, a column with intermittent welds retains essentially all the local buckling resistance of a column with continuous welds.
2. However, columns with intermittent welds cannot take the abuse of those with continuous welds, and will buckle locally if the average stress is slightly more than the yield point.
3. Statements similar to (1) and (2) likewise apply to beams even though in certain cases the intermittent welds are carrying rather large shears.

It is recommended that consideration be given to revising specifications to include the following ideas:

1. Limiting the permissible gap of intermittent fillet welds to *16t* does not appear to have a very sound and logical basis. From this program, it would seem more rational to limit the gap by using the critical gap-width ratio criterion.
2. In view of the fact that members with intermittent welds will not take much abuse in excess of the yield point, perhaps the use of intermittent welds should be restricted to secondary members.

The use of intermittent welds appears to be undesirable both from the viewpoint of local buckling resistance and of fatigue properties. Thus, it seems that *light continuous* welds should be used instead in cases where *intermittent full size* welds are being considered.

ACKNOWLEDGMENTS

We acknowledge with appreciation the support and assistance of W. Spraragen,

Figure 11

Table 5—Series E, Triangular Load, Both Long Edges Simply Supported

Test No.	*Thickness t, in.*	*Gap g, in.*	*Support length, in.*	*Av. stress ultimate load, ksi*
	b = 3.75 in.; E = 29,800 ksi; yield point = 41.4 ksi			
302	0.035	0	Cont.	21.42
307	0.035	0	Cont.	20.86
310	0.035	0.75	3	20.67
308	0.035	1	3	18.35
309	0.035	1	3	18.61
306	0.035	1.5	3	16.36
305	0.035	1.5	3	16.46
304	0.035	1.75	3	13.98
303	0.035	1.75	3	14.96
	b = 6.25 in.; E = 29,400 ksi; yield point = 41.1 ksi			
262	0.126	0	Cont.	27.07
263	0.125	0	Cont.	26.59
264	0.125	1.75	3	26.18
265	0.125	1.75	3	25.13
266	0.124	3.25	3	26.86
267	0.125	3.25	3	25.13
270	0.125	4.75	3	19.90
271	0.125	4.75	3	22.11
268	0.125	7.75	3	14.66
269	0.127	7.75	3	14.52
	b = 3.75 in.; E = 29,400 ksi; yield point = 41.1 ksi			
246	0.128	0	Cont.	25.75
247	0.125	0	Cont.	26.70
248	0.126	1.25	3	26.70
249	0.125	1.25	3	25.42
250	0.127	3.25	3	23.92
251	0.124	3.25	3	23.13
252	0.126	7.75	3	13.51
253	0.126	7.75	3	14.44
	b = 3.75 in.; E = 31,000 ksi; yield point = 30.3 ksi			
201	0.186	0	Cont.	18.25
202	0.186	0	Cont.	19.35
205	0.186	0	Cont.	18.04
203	0.185	1.75	3	18.14
204	0.185	1.75	3	17.23
206	0.186	3.25	3	18.25
207	0.186	3.25	3	19.35
208	0.186	4.75	3	18.25
209	0.186	4.75	3	19.35
210	0.184	7.75	3	16.83
211	0.184	7.75	3	16.83

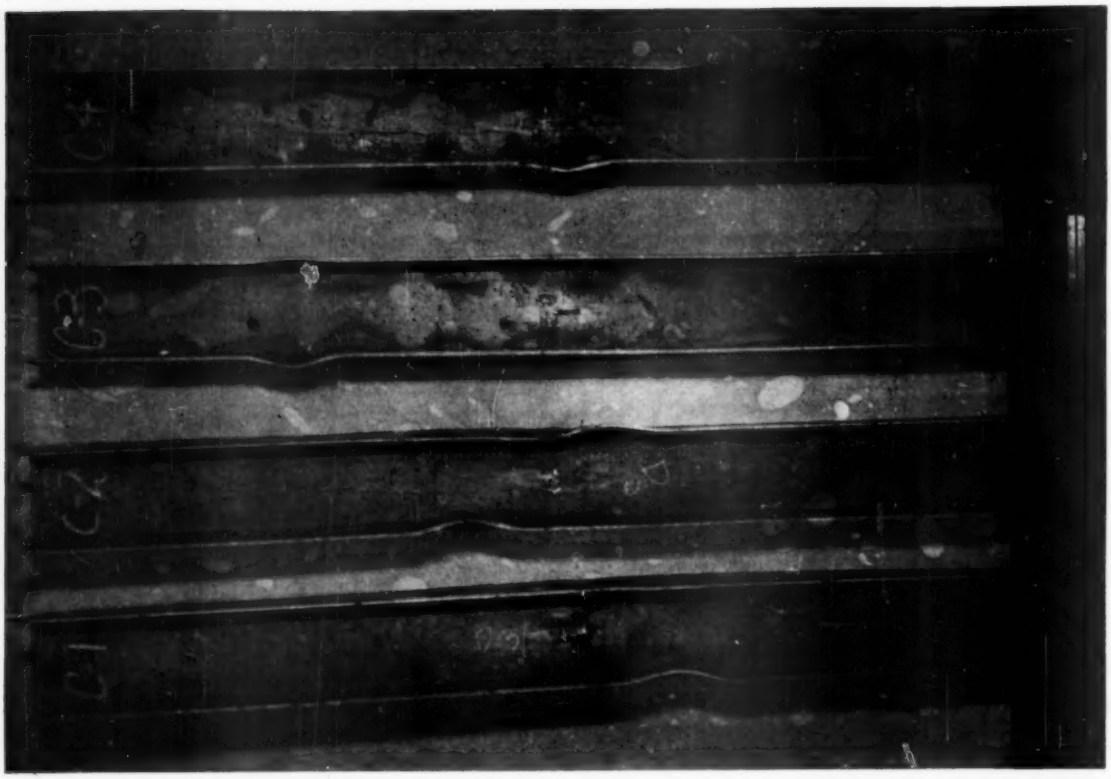


Figure 12

Figure 13

Director of the Welding Research Council, LaMotte Grover, Chairman of the Structural Steel Committee, and Oliver G. Julian, Chairman of Project Subcommittee, in the conduct of this investigation.

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Appendix A

BUCKLING OF INTERMITTENTLY SUPPORTED RECTANGULAR PLATES UNDER TRIANGULARLY DISTRIBUTED END COMPRESSIONS

In a previous paper entitled "Buckling of Intermittently Supported Rectangular Plates," a rational theory with supporting experimental evidence was presented to explain the buckling behavior of such plates under *uniformly distributed end compressions*. The following summarizes a theoretical and experimental supplement to the earlier paper, extending the discussion to include the buckling behavior of similar plates under *triangularly end compressions*.

The extension of the previous theory may be developed along similar lines to that presented in Appendix A of the previous paper and will not be discussed in detail here. It should be noted, however, that this derivation applies to relatively long rectangular plates which are loaded in their plane by compressive forces on the two ends and intermittently supported along the two long edges. The ends of the plate are considered to be simply supported. The long edges are not loaded in the plane of the plate and are considered to be supported at points which are uniformly spaced either one or both edges. The spacing of these intermittent point supports is called the gap g.* It is assumed that the material is homogeneous and elastic, and follows Hooke's law; that the plate is free from initial curvature or eccentricities; and that the deflections are small.

The buckling of rectangular plates subjected to a uniform compression on both ends was first studied by Bryan.[2] Bryan's formulas were subsequently extended by Timoshenko[3] and others.[4, 5, 6] All of these solutions apply to relatively long rectangular plates of length a, unsupported width b and thickness t; and to cases in which one or both long edges are *continuously* supported in various manners. Bryan's formula may be expressed as follows:

$$\sigma_{cr} = K \frac{\pi^2 E t^2}{12(1 - \nu^2)b^2} \qquad \text{(a)}$$

where

σ_{cr} = critical stress, i.e., average axial stress caused by the buckling load, and prevailing just prior to buckling.

Table 6—Series F, Triangular Load, One Long Edge Simply Supported; One Free, Peak Load at Free Edge

Test No.	*Thickness t, in.*	*Gap g, in.*	*Support length, in.*	*Av. stress ultimate load, ksi*
	b = 3.4375 in.; E = 30,000 ksi; yield point = 41.2 ksi			
212	0.096	0	Cont.	9.50
213	0.096	0	Cont.	12.50
217	0.096	3.25	3	10.61
218	0.096	3.25	3	11.81
272	0.096	4.75	3	10.50
273	0.098	4.75	3	8.33
221	0.096	6.25	3	8.25
222	0.096	6.25	3	8.50
220	0.094	7.75	3	6.89
	b = 4.0625 in.; E = 30,700 ksi; yield point = 35.1 ksi			
214	0.170	0	Cont.	14.52
227	0.170	0	Cont.	15.97
236	0.168	4.75	3	14.73
237	0.169	4.75	3	14.47
216	0.170	6.25	3	13.92
228	0.171	6.25	3	15.13
215	0.168	7.75	3	13.47
229	0.167	7.75	3	14.78
	b = 4.0625 in.; E = 29,900 ksi; yield point = 30.0 ksi			
254	0.309	0	Cont.	21.30
255	0.310	0	Cont.	21.90
256	0.311	4.75	3	20.95
257	0.311	4.75	3	20.80
259	0.309	6.25	3	20.34
260	0.310	6.25	3	18.58
258	0.311	7.75	3	20.10
261	0.310	7.75	3	20.57

Table 7—Series F, Triangular Load, One Long Edge Simply Supported; One Free, Peak Load at Supported Edge

Test No.	*Thickness t, in.*	*Gap g, in.*	*Support length, in.*	*Av. stress ultimate load, ksi*
	b = 3.4375 in.; E = 30,000 ksi; yield point = 41.2 ksi			
230	0.096	0	Cont.	22.77
226	0.096	0	Cont.	20.49
234	0.096	1.75	3	19.99
235	0.096	1.75	3	21.77
225	0.098	3.25	3	18.88
274	0.096	4.75	3	16.16
275	0.096	4.75	3	15.69
231	0.098	7.75	3	9.06
232	0.097	7.75	3	8.41
	b = 4.0625 in.; E = 30,700 ksi; yield point = 35.1 ksi			
239	0.169	0	Cont.	21.77
277	0.168	0	Cont.	25.47
244	0.168	3.25	3	24.63
245	0.168	3.25	3	23.27
240	0.169	4.75	3	21.07
241	0.169	4.75	3	21.30
242	0.169	7.75	3	17.62
243	0.170	7.75	3	16.77
	b = 4.0625 in.; E = 29,900 ksi; yield point = 30.0 ksi			
278	0.311	0	Cont.	24.18
279	0.310	0	Cont.	24.70
282	0.311	3.25	3	24.69
283	0.311	3.25	3	23.67
280	0.310	4.75	3	21.68
281	0.310	4.75	3	22.71
284	0.310	7.75	3	21.46
285	0.310	7.75	3	20.05

* When considering actual plates having intermittent welds of finite length along the edges, the gap g should be considered as the *clear* distance between intermittent welds.

K = a constant depending on the type of support along the long edges, and the manner in which the end compressions are distributed.
E = modulus of elasticity in compression.
ν = Poisson's ratio.

The values of K for a rectangular plate having a *length four or more times its width* are as follows, to the nearest tenth:

Uniformly distributed end compressions, and	
Both edges clamped	$K = 7.0$
Both edges simply supported	$K = 4.0$
One edge clamped, one free	$K = 1.3$
One edge simply supported, one free	$K = 0.5$
Triangularly distributed end compression, and	
Both edges clamped	$K = 6.8$
Both edges simply supported	$K = 3.9$
One edge clamped, one free, peak load at clamped edge	$K = 3.2$
One edge clamped, one free, peak load at free edge	$K = 0.8$
One edge simply supported, one free, peak load at supported edge	$K = 0.9$
One edge simply supported, one free, peak load at free edges	$K = 0.3$

In the previous paper, the theory for intermittently supported plates subjected to uniformly distributed end compressions showed that the critical stress could be evaluated using eq (a) provided the proper value of K was used. It was shown that, up to a certain gap-width ratio (called the critical gap-width ratio), an intermittently supported rectangular plate retains all the buckling resistance of a continuously supported plate, but above this critical gap-width ratio, an intermittently supported plate buckles in the gap as if the plate acted as a pinned end column of a length equal to the gap and with Youngs' modulus E replaced by $E/(1-\nu^2)$. For example, a plate having both long edges intermittently supported in a simple manner had a critical gap-width ratio of 0.5. Below this ratio, the critical stress could be obtained from eq (a) using 4.0 for the value of K; above this ratio, however, K had a value equal to $(b/g)^2$.

The extension of the previous theory to the case of triangularly distributed end compressions leads to the same conclusion as summarized above for uniformly distributed end compressions. The value of the critical stress in the case of plates having various edge conditions and subjected to *triangular end compressions* may be obtained from eq (a) using the values of K shown in Fig. 4. In any particular case, the critical gap-width ratio is obtained from the intersection of the horizontal line, representing the K of a similar continuously supported plate, with the $(b/g)^2$ curve, representing the K for column-type buckling in the gap. The values of the critical gap-width ratios obtained in this manner have been summarized above in Table 1.

Figure 14

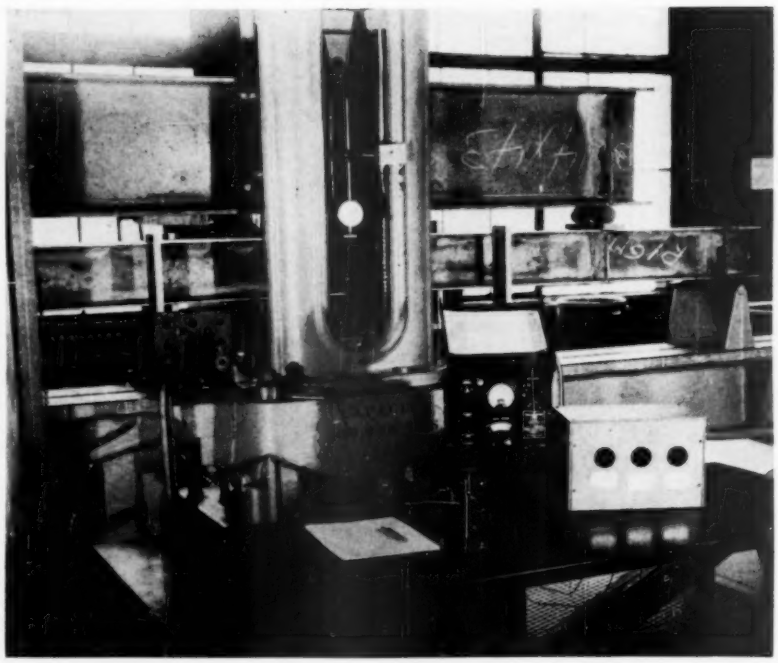

Figure 15

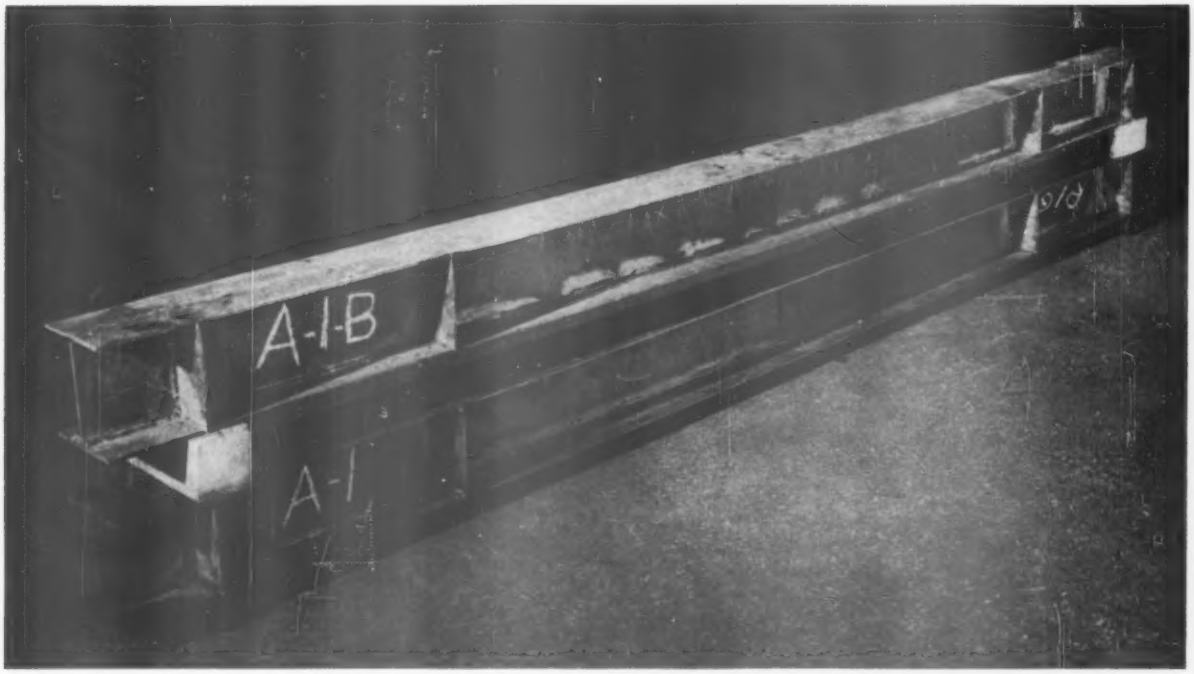

Figure 16

For any particular type of edge support, the theoretical values of the critical buckling stress may be represented by a family of curves such as shown in Fig. 5. This particular family has been drawn for the case of both long edges having intermittent simple supports and a triangular end compression. The solid-line curves give the values obtained using eq (a) and the values of K from Fig. 4. Of course, these curves are not valid and should not be used if the maximum stresses exceed the proportional limit. While the following assumption is subject to criticism, it has been assumed that the solid-line curves are valid up to a σ_{cr} equal to 0.5 of the yield point stress, σ_{bp}. (Note that, σ_{cr} being the average axial stress, when it is 0.5 σ_{yp}, the maximum axial stress is at least σ_{yp} and far in excess of the proportional limit.) The dashed-line curves representing the behavior above the proportional limit have been drawn in the same manner as was done for similar curves in the previous paper.

The validity of this correction procedure as well as the adequacy of the theoretical solution may now be investigated by comparing the values obtained from curves such as Fig. 5 with test results. For this purpose, three series of tests—Series E, F and G—were conducted.

These tests were conducted using essentially the same fixtures as those for Series A, B, C and D reported on in the previous paper. The only change involved the so-called "flexible knife edge" which was shown in Fig. 8 of the previous paper. For Series E, F and G, the areas of the ends of the plunger in contact with the confined rubber were varied so as to achieve a triangular load distribution across the knife edge. The support condition on the long edge of the plates for these series were arranged as follows:

Series E—Both edges simply supported.
Series F—One edge simply supported, one edge free, peak load at free edge.

Figure 17

Figure 18

Series G—One edge simply supported, one edge free, peak load at supported edge.

In Series E, four groups of plates, each having a different width-thickness ratio, were tested and within each group the gap-width ratio was varied. In Series F and G, three groups instead of four were tested. The detailed dimensions of the plates in Series E, F and G are summarized in Tables 5, 6 and 7, respectively.

In these three series, the detailed procedure of conducting the tests was the same as reported in the previous paper and will not be described herein. Figures 6 and 7 show the appearance of typical plates from Series E and G, respectively, after testing. The average stress at the ultimate load sustained by each of the plates is likewise recorded in Tables 5, 6 and 7. The critical buckling load and the ultimate load are essentially identical for the relatively low width-thickness ratios involved in the tests. Therefore, the average stress at ultimate load was plotted as the experimental value of the critical stress in the comparison of experiment and theory shown in Figs. 8, 9 and 10.

The experimental values for Series E, F and G are represented by the encircled points plotted in Figs. 8, 9 and 10, respectively. The dot-dash curves shown in these figures have been faired through the test points. The partially dashed- and partially solid-line curves, however, represent a cross plot of the theoretical solution obtained from families of curves such as Fig. 5. The dashed-portion indicates that the points for plotting such portions were obtained from the dashed portion of curves such as shown in Fig. 5.

Heavy solid vertical lines have been drawn on the plots in Figs. 8, 9 and 10 at the theoretical values of the critical gap-width ratio. Heavy dashed vertical lines have been drawn at the gap-width ratio which corresponds to a gap which is 16 times the thickness of the plate—that is, the maximum gap allowed by most specifications.

As in the previous paper, it is significant to note that the experimental curves are essentially straight and horizontal up to the vicinity of the theoretical critical gap-width ratio and then break down to the right. It is true, of course, that the ordinates of the theoretical and experimental curves do not agree too well in most cases. Close agreement cannot be expected, however, since the ultimate buckling strength depends upon imperfections of the plate, initial curvature, length of intermittent support, etc.—none of which are taken into consideration in the theory used herein.

The primary objective of the theoretical development was to determine if there was some critical gap-width ratio below which an intermittently supported plate retains essentially all the buckling strength of a continuously supported plate. It is believed that these tests with the triangular end compressions—just as the previous tests with the uniform end compressions—do confirm the buckling behavior predicted by the theory and show that the theoretical critical gap-width ratios are quite accurate.

It is also of interest to note that in none of the ten sets of curves shown in Figs. 8, 9 and 10 has there been a marked decrease in buckling resistance up to the gap-width ratios corresponding to 16 times the thickness. It is true that in the low width-thickness groups these gap-width ratios are in excess of the critical gap-width ratios. For such cases, however, the buckling resistance decreases slowly as the gap increases, and, therefore, gaps in excess of the critical gap-width ratio can be tolerated.

Crack Propagation

Crack propagation due to mechanical action is surveyed and the relationship of the fracture toughness to tensile strength and work hardening is developed

by Frederick Forscher

Abstract

The literature on crack propagation is surveyed. Crack propagation due to mechanical action is reported and analyzed by means of a modified Griffith's crack mechanism. A material property, so-called "fracture toughness," is of major importance; it equals the total energy (surface energy plus energy of plastic deformation) needed to form a pair of new unit surfaces; it also equals the driving force needed per unit length of crackfront. The relationship of the fracture toughness to the tensile strength and the work hardening coefficient is developed.

A mathematical model is reported based on a mechanism which assumes Griffith cracks to grow while at the same time new ones are formed (ahead of the moving main-crack front) which, in turn, grow and eventually join with the earlier cracks to form the fracture surface.

INTRODUCTION

ONE of the oldest and yet unsolved problems in the field of Mechanics of Materials is the question, "Why, when and where does a material break?" Depending on the viewpoint or preference of the investigator, answers have been forthcoming in terms of atomistic processes, microscopic mechanisms or macroscopic stress criteria. To illustrate these three view points we first consider the material to be made up of an assembly of ordered atoms in which case the integrated cohesive bond strength should be the measurable cohesive strength of the material. Next we may consider the material to contain defects of such a nature that stress concentrations at the sharp edges of the defects tend to enlarge them when a properly oriented external stress is applied (Griffith's crack theory). Finally we may consider the material to be isotropic, homogeneous throughout and able to resist load until the maximum stress, as computed according to the Theory of Elasticity, reaches a critical value (Guest's Maximum Stress Hypothesis). It is clear that each of these viewpoints has some merit.

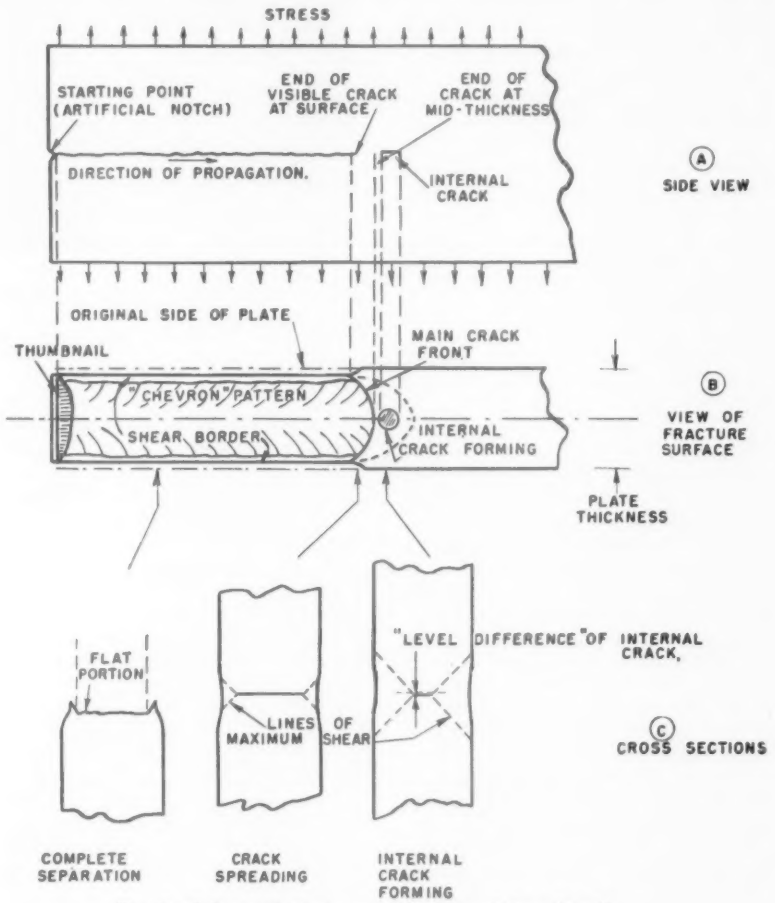

Fig. 1 Schematic features of fracture (after Boyd[8])

It is now well understood that fracture is not an instantaneous process. It usually starts slowly in one or more places within the material, usually in regions of high stress and/or concentrations of defects, and eventually propagates rapidly until the driving energy is dissipated. One school of thought regards the fracture problem as the prevention of the initiation of the crack, while another school of thought realistically considers every material to contain defects of one kind or another and thus the fracture problem becomes the prevention of the propagation of the crack. These considerations are reminiscent of an undesirable nucleation and growth phenomenon which may be arrested by preventing either nucleation or growth. In the present report the emphasis is placed on the propagation of the crack while its initiation, still veiled in mystery, is taken for granted. These questions are to be answered: how is the fracture being formed and under what condition will a crack become self-propagating?

The mechanism of crack propagation has only lately received some scanty attention.[1-6] Analytical work is still far

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behind experimental evidence, though both approaches have already produced important contributions to the understanding of fracture. The background material for this investigation is found in the voluminous literature on the flow and fracture of solids, particularly solid metals.

ENERGY OF CRACK PROPAGATION: MECHANICAL VS. CHEMICAL

A fine experimental study of the sequence of events by which a crack "grows" is reported by Kies, Sullivan and Irwin.[1] The study clearly showed that fracture is a process of continuous occurrence of essentially discontinuous initiations, Fig. 1. Microcracks form ahead of the main fracture front and spread approximately radially until they meet other spreading microcracks or the advancing fracture front. Level difference between the microcracks and the advancing fracture front give rise to clearly visible markings (so-called "Chevrons"), Fig. 2. Some of the conclusions of Kies, Sullivan and Irwin are as follows:

1. The over-all velocity of propagation of a crack is governed by the rate and distance ahead of the fracture front at which advance initiations can occur and the rapidity with which subsidiary multiple fractures can be joined up.

2. The transition from slow to fast fracture is accomplished by finer and finer scaled elements initiating closer and closer to the leading edge of the main crack as the local velocity of propagation increases. (Here a "slow fracture" is one that may be slowed down during its progress by unloading the machine or rearranging the loading, while the speed of a "fast fracture" may be considered beyond experimental control.)

These experiments illustrate the mechanism of crack propagation if strain energy, applied or stored, is the source of the driving force. If corrosion is the cause of the cracking a different mechanism of propagation must be considered in which the chemical energy (electrochemical effects) is the source of the driving force. Usually these two mechanisms are present simultaneously to a larger or smaller extent. Suppose a relationship can be found between the surface chemical energy (corrosion rate, wetting action, etc.) and the driving force of a crack. The driving forces due to mechanical and chemical energies would be simply additive if the principle of superposition would hold; however, it is not at all certain that the rate of crack advance due to corrosion and mechanical effects equals the sum of the two individual rates. The following analysis will deal with mechanical energy as a driving force only. Corrosion effects, if present, are neglected. In general they will increase the rate of crack advance. Whether this increase will be imperceptible or will reach dangerous proportions in a given case cannot be predicted at the present time.

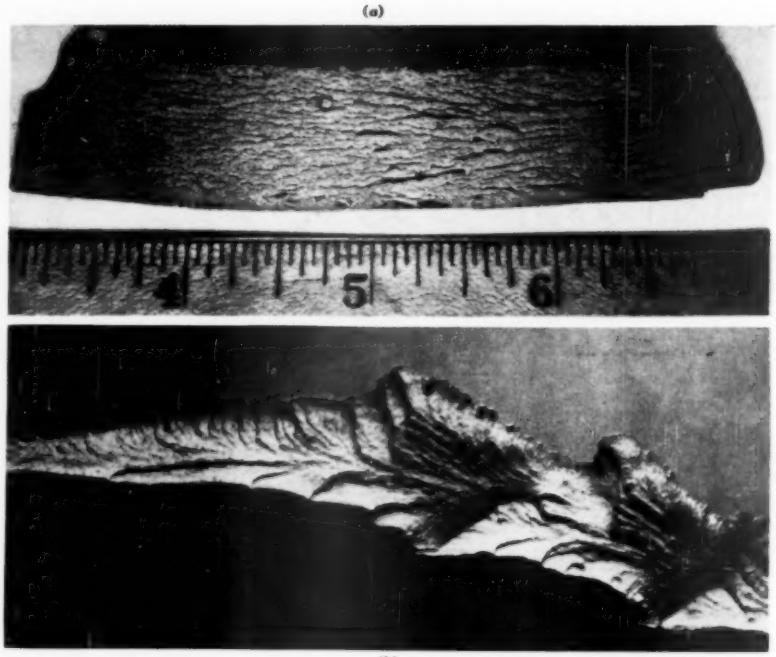

Fig. 2 Chevron markings. (a) Minor chevron markings. Brittle fracture on a plane perpendicular to plate surface. (b) Irregular chevron markings. Brittle fracture on a surface approximately inclined 45 deg to plate surface (same magnification as above)

STARTING OF A SINGLE CRACK IN AN INFINITE SOLID

It is customary to approach the problem of fracture on the microscopic level by the introduction of Griffith's cracks.[15] This concept provides a convenient mechanism by which it is possible to explain why, under certain stress conditions, certain existing defects in the metal will grow to dangerous size and ultimately cause fracture. Griffith assumed that a crack arising from a defect (so-called Griffith's crack) would grow cataclysmically when the required energy needed in the formation of the new surface is less than the liberated strain energy due to the spread of the crack.

It has been repeatedly pointed out[6] that we are not justified in applying Griffith's theory *per se*, to the problem of fracture of metals because the original theory allows for no plastic deformation, while actually such deformation always accompanies fracture in metals. To circumvent this difficulty some authors have interpreted "the energy needed in the formation of new surfaces" to mean not only surface energy, as originally envisioned by Griffith, but also the much larger quantity of energy needed for plastic deformation of material adjacent to the newly formed surface. We shall call this quantity fracture toughness and denote it by W_c with dimensions of energy per unit area of fracture, or equivalently, force per unit length of crack front. This quantity is a characteristic of the material and its temperature.*

It is apparent that W_c will be the larger the deeper plastic deformation penetrates from the fracture surface into the bulk of the material. At lower temperatures this penetration is usually smaller than at higher temperatures. The rate by which plastic deformation can spread must also have some effect on W_c, and since that rate is temperature sensitive it follows that $W_c = f(T)$. The lower limit of W_c is the special case when no plastic deformation at all takes place so that $W_c = \gamma$ where γ is the surface energy of the metal. This is the case to which Griffith's original calculations apply. (Surface energies of metals range between 300 and 3000 dynes/cm or erg/cm^2.)

The writer proposes that the critical size of a Griffith crack or equivalently the critical stress σ_f under which a given crack will spread can be derived by dimensional analysis without recourse to the Theory of Elasticity. The circular crack will start to spread when the total energy of the system is thereby decreased. The pertinent physical quantities in this process are assumed to be: elastic energy per unit volume, W_e; diameter of crack, b; fracture toughness (defined above), W_c.

* This idea was already expressed by M. Moser[14] in 1924 who considered as two fundamental properties: (a) the maximum amount of work that can be absorbed by a unit volume of material, and (b) the velocity of plastic strain propagation.

In terms of the fundamental dimensions of length (L), time (T) and force (F) we can express the above quantities as:

$$W_e = LFL^{-3}$$
$$b = L$$
$$W_c = LFL^{-2}$$

Applying Buckingham's theroem, we find $n = 3$ (pertinent quantities) and $m = 2$ (dimensions), hence $n - m = 1$ independent dimensionless group can be formed:

$$\pi_1 = \frac{W_c}{W_e b}$$

Remembering that elastic energy per unit volume (W_e) is proportional to stress times strain

$$W_e = \sigma\epsilon \approx \frac{\sigma^2}{E}$$

where E is Young's Modulus, the dimensionless group becomes

$$\pi_1 = \frac{EW_c}{\sigma^2 b} = \text{constant}$$

from which we find

$$\sigma \approx \sqrt{\frac{EW_c}{b}}.$$

Instability is reached under a stress at which the material rather propagates the crack than stores more elastic energy. That stress is called the fracture stress (σ_f); it can be so defined that the constant of proportionality becomes equal to one.

$$\sigma_f = \sqrt{\frac{EW_c}{b}} \qquad (1)$$

The meaning of this relation will now be illustrated by means of a numerical example. Consider the case for an ideally brittle fracture where $W_c = \gamma$ and assume $\gamma = 1000$ erg/cm² * as an average value. Let $E = 10^{12}$ dyne/cm² ($14.5 \times (10)^6$ psi) and let the largest crack length be approximately equal to a subgrain dimension, i.e., $b = 0.5\ \mu = 5\,(10)^{-5}$ cm.

From 1 we find

$$\sigma_f = \sqrt{\frac{(10)^{-2}\ 10^3}{5(10)^{-5}}} = 0.45\,(10)^{10} \text{ dyne/cm}^2 = 65{,}000 \text{ psi}$$

For a ten-times larger defect (subgrain) we have $b = 5\ \mu$ and find $\sigma_f = 20{,}500$ psi. Defects that are smaller than 0.5 μ (5000 Å) are below the visibility limit of an optical microscope. But only such defects could give rise to brittle fracture stresses (σ_f) which approach values (of 100,000 to 200,000 psi) encountered in practice.

In order to apply eq (1) to a case where plastic deformation does take place, we have to estimate W_c by assuming a thickness of the plastic layer adjacent to the fracture surface, the amount of plastic deformation which this layer suffered, and the yield stress that caused it. Following Orowan[7] we assume a $^1/_4$ mm thickness of metal on each side of the fracture surface is deformed 2% plastically by a steady yield stress of say 29,000 psi. From this W_c is computed as follows:

$$W_c = \gamma + 2[2(10)^{-2} \text{ cm/cm } 2\,(10)^9 \text{ dyne/cm}^2\ 1/40 \text{ cm}]$$
$$= \gamma + 2(10)^7 \text{ erg/cm}^2 \sim \gamma + 100 \text{ in.-lb/in.}^2 \text{ *}$$

The contribution of the assumed plastic deformation to W_c is about 10,000 times greater than the surface energy γ. Consequently γ may be neglected in such cases. For the case of $b = 5\ \mu$ eq (1) yields $\sigma_f = 650{,}000$ psi, a result of unreasonable magnitude. We must conclude that (assuming the material is capable of such plastic deformation) defects of the order of 5 μ are harmless. Defects ten times larger (50 μ) bring the computed fracture stress into the range of practical magnitudes (205,000 psi).

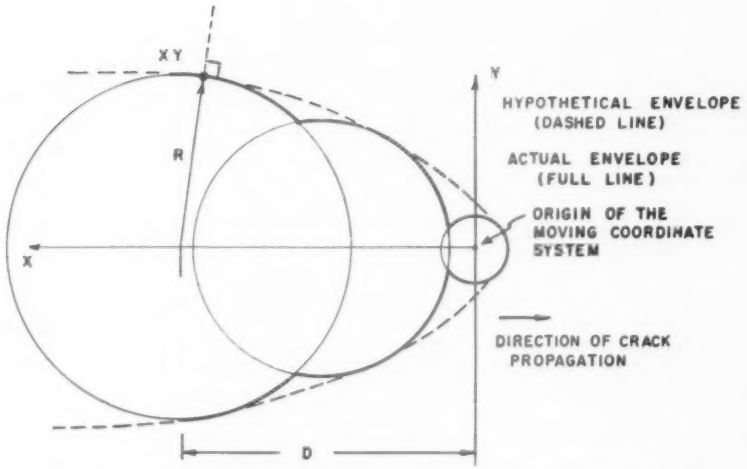

Fig. 3 Geometry used in the analysis of crack propagation (after Boyd[5])

PROPAGATION OF ONE CRACK IN AN INFINITE SOLID

Equation (1) along with the foregoing discussion gives no indication of how fast the crack spreads; it only indicates that the infinite solid containing a flaw b will begin to fracture when σ_f is applied. The propagation of the crack, in opposition to its start, will depend on the continuous supply of W_c dynes/unit length of crack front or ergs/unit area of new fracture surface; the larger the crack front the more total energy must be supplied in order to keep it propagating.

If we consider radially spreading internal cracks of radius r in an infinite solid, we find that the input energy (W) per unit time expended on crack propagation is proportional to the circumference of the crack, since

$$\frac{dW}{dt} = W_c \frac{dA}{dt} = 2\,\pi\,r \frac{dr}{dt} W_c \qquad (2)$$

If the crack enlarges with a constant rate dr/dt, energy must be added at an increasing rate. On the other hand a constant rate of energy input $dW/dt = C$ causes the circular crack front to slow down. Assuming $dW/dt = C$ and integrating eq (2) gives

$$r^2 = \frac{Ct}{W_c \pi} \qquad (3)$$

where $r = 0$ at $t = 0$. Boyd[5] assumes that dW/dt tends to a constant value analogous to a "natural" limit such as the terminal velocity of bodies falling through a fluid. The justification of such an assumption may appear later on in the discussion of the temperature effect on W_c. We are now in a position to combine the experimental findings, that fracture is a process of continuous occurrence of essentially discontinuous initiations, with the previous analysis. In the following analysis due to Boyd[5] a mechanism is described mathematically in which not only one circular crack grows, as before, but in which new cracks are formed along one direction designated as the x-axis. The axis is assumed to lie in the plane of all the forming cracks except for slight level differences which account for the chevron markings. It is desired to develop an expression for the crack propagation in a plate in which the x-axis will be the centerline of the crack as well as of the plate. In Fig. 3 are shown three circular cracks in the course of their propagation. The envelope to these circles may be considered the main crack front. If the circles are spaced closer together the envelope approaches a continuous function for which a mathematical expression may be found. It is assumed that the initiation of new cracks proceeds with constant rate V_c along the x-axis. If the cracks would grow with constant velocity the resulting envelope would consist of two

* This surface energy corresponds roughly to 1 ev per atom.

* A Charpy V-notch value of 1 ft-lb corresponds to 100 in.-lb/in.² if it is assumed that the energy is absorbed uniformly over the 80-mm² cross section.

straight lines. However, Boyd assumes that each crack is supplied with a constant energy rate (dW/dt) in which case eq (3) applies; i.e., the radius of each crack grows with a decreasing velocity and thus the envelope curve will not be straight. It should be pointed out that Boyd makes no allowance for the area which is common to two adjacent cracks after they have grown big enough to overlap.

An arbitrary point (x, y) on the envelope, Fig. 3, will move in the direction R as the main crack front progresses to the right. The direction of R is found by erecting a normal to the envelope (or its tangent) at the point x, y. Following Boyd[5] we introduce a moving coordinate system with its origin at the most recent circular crack; an observer moving along with that coordinate system would be at rest relative to the envelope and thus the envelope $y = f(x)$ is independent of time.

From the geometry of Fig. 3 we find

$$R^2 = y^2 + (D - x)^2$$

$$\text{and } D = x + y\frac{dy}{dx} \quad (4)$$

But according to the assumption

$$\frac{dD}{dt} = V_c \quad (5)$$

and

$$R^2 = -\frac{Ct}{W_c\pi}$$

according to eq (3). Eliminating R^2, D and t from the above four equations leads to

$$\frac{C}{W_cV_c\pi}\left(x + y\frac{dy}{dx}\right) = y^2\left[1 + \left(\frac{dy}{dx}\right)^2\right]$$

which is the differential equation of the envelope. (In the derivation, the constant of integration of eq (5) is assumed to be zero.) The solution to this equation is given by

$$y^2 = \frac{C}{W_c\pi V_c}\left[x + \frac{C}{4W_c\pi V_c}\right] = 4a(x + a)$$

where

$$a = \frac{C}{4\pi W_cV_c} \quad (6)$$

The envelope is therefore a parabola with focal length a and the origin of the coordinate system at the focus. It is seen that increasing the velocity V_c of crack propagation or increasing the fracture toughness W_c of the material decreases the focal length; i.e., the parabola becomes "sharper."

The observation that level differences between the microcracks and the advancing main crack front give rise to chevron markings, can now be mathematically expressed. A level difference between two adjacent microcracks must propagate with the advancing crack front once the two microcracks have joined; thus the level difference traces a curve which is at any instant perpendicular to the main crack front, Fig. 4; in other words chevrons must form orthogonal trajectories to eq (6). Hence

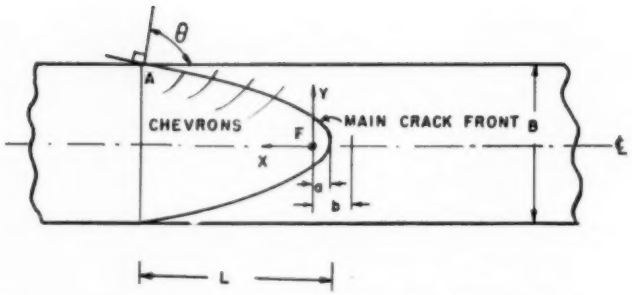

Fig. 4 Geometry used in the analysis of crack propagation in a plate (after Boyd[5])

$$\frac{dx}{dy} = \frac{1}{dx_1/dy_1} = \frac{y}{2a} = \frac{y_1}{2a} \quad (7)$$

where the subscript denotes the new variables of the chevrons. Integrating eq (7) gives

$$y_1 = 2ae^{-\frac{x_1}{2a}} \quad (8)$$

under the condition that the moving focal point is the origin for both coordinate systems, i.e., at

$$x = 0, \quad x_1 = 0, \quad y_1 = 2a\,.$$

Summarizing the last section, it was assumed that crack propagation proceeds under constant velocity, V_c, and constant energy dissipation per unit time, C. These assumptions lead to two families of curves; these *families* can be visualized by moving the curves in Fig. 4 along the negative x-axis N times a distance b.

For the envelopes

$$y^2 = 4a(x + Nb) \quad (9)$$

For the chevrons

$$y_1 = 2aexp\left[\frac{-(x_1 + Nb)}{2a}\right] (10)$$

where N is an integer and b is the spacing of the chevrons in the direction of the x axis.

PROPAGATION OF A CRACK IN A PLATE

Boyd applied the foregoing analysis to the case of a plate of thickness B. It is clear that the crack is more advanced in the center of the plate than at the edges due to its parabolic shape. This is experimentally verified by Tipper[8] and Robertson[9] who also observed advanced microcracks which have not yet joined up with the main crack front.

The advance L, Fig. 4, of the nose of the crack over its appearance on the side of the plate is given by $L = x + a$.

If we let $y = B/2$ in eq (6) we find

$$L = \left(\frac{W_c\pi V_c}{4c}\right)B^2 = \frac{1}{16a}B^2 (11)$$

The angle θ at which the crack front intersects the side of the plate (Point A in Fig. 4) is found from eq (7).

$$\tan\theta = \frac{B}{4a} = \frac{W_c\pi V_c}{C}B \quad (12)$$

The underlying assumptions, as pointed out above, are constant toughness W_c and constant rate at which work is being done on the plate; which means that the analysis applies to the "steady state" crack propagation but not to the initiation nor to the arrest of the crack. In our case

$$W_c\frac{dA}{dt} = W_c(V_cB) = C \quad (13)$$

Combining this equation with eq (12) leads to the surprising prediction that

$$\tan\theta = \pi \quad \text{or} \quad \theta \approx 72° \quad (14)$$

which should be independent of the thickness and the properties of the material. Boyd[5] reports good agreement with experimental results. Measurements on 20 specimens ranged from 63° to 77° with an average of 69°.

INVESTIGATION OF THE FRACTURE TOUGHNESS W_c

Fracture toughness greatly depends on the depth to which plastic deformation can penetrate before the advancing crack removes the load on this particular portion of the cross section. The deeper plastic deformation penetrates, the larger is the fracture toughness W_c. On the other hand localization of plastic deformation decreases W_c. It is clear that crack

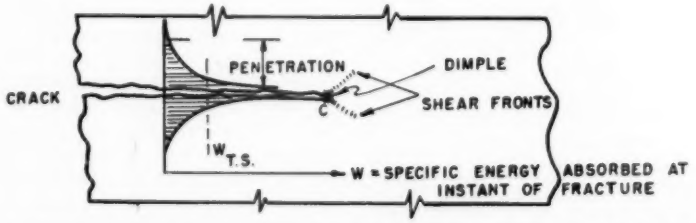

Fig. 5 Some features of an arrested crack in a ductile plate and hypothetical energy absorption

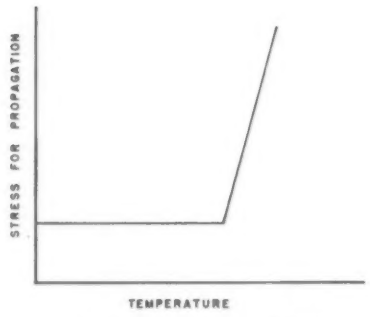

Fig. 6 Schematic results of Robertson's[9] tests

propagation through a plate containing a temperature gradient must be affected by the variation of W_c as a function of temperature. Thus, Robertson[9] investigates experimentally the temperature at which a running crack is arrested. He applies a uniform tensile stress across a wide plate specimen with an artificial (saw-cut) crack starter. A bolt gun impulse starts the crack moving. Repeating the experiment under different combinations of applied stress and temperature, Robertson establishes an experimental relationship between temperature and the lowest stress that still causes rapid crack propagation, Fig. 6. There exists a well-defined temperature above which the stress necessary for crack propagation rapidly increases. Below this temperature the lowest stress for crack propagation is a fraction of the yield strength of the material. The position and knee in the curve, Fig. 6, are slightly modified by the thickness of the plate. Measured crack velocities depend on stress and temperature and vary between 1000 to 6000 fps.

In order to find expressions of more general applicability for W_c the writer assumes that the material follows the "n-power" law

$$\sigma = k\delta^n \qquad (15)$$

in which k can be expressed in terms of the engineering tensile strength[12] by

$$k = \sigma_{ts}\left(\frac{e}{n}\right)^n \qquad (16)$$

where:

δ = natural strain
n = work hardening coefficient
e = base of the natural logarithm
σ_{ts} = engineering tensile strength (max load divided by original area)
σ = true stress

The work-hardening coefficient n is influenced by temperature as well as speed of testing. No definite relationship is known for this variation. Generally n decreases with increasing temperatures[16] if the mechanism of deformation is not altered by metallurgical effects (precipitation, grain growth, recovery, phase changes, etc.). The effect of strain rate on n is only pronounced at temperatures where creep becomes significant. At such temperatures n increases with increasing strain rate. It is important to understand that the n-power law itself is an empirical relationship which cannot be extrapolated to very large or very small values of stress and strain. Its applicability has to be experimentally verified in every given case.

The point of instability in a tensile test is reached when $\delta = n$, i.e., the lower the work-hardening coefficient the sooner in the stress-strain history will necking occur.

The work per unit volume that is needed to deform the material to the instability point, or equivalently, the work per unit volume needed to reach the ultimate strength is

$$W_{ts} = \int_0^{\delta=n} \sigma\, d\delta = \int_0^n k\delta^n\, d\delta = \sigma_{ts}\frac{n}{n+1}\, e^n$$

Since n is generally smaller than unity, it is advantageous to express the exponential in series form and drop all powers of the second and higher order.

$$W_{ts} = \sigma_{ts}\frac{n}{n+1}\left(1 + n + \frac{n^2}{2!} + \frac{n^3}{3!} + \right)$$

$$W_{ts} = \sigma_{ts} n(T, \dot{\epsilon}) \qquad (17)$$

The symbols in the bracket express the fact that both quantities, and hence W, are temperature (T) and strain rate ($\dot{\epsilon}$) dependent. For σ_{ts} we could substitute the Hollomon-Zener[10] relation

$$\sigma_{ts} = \text{const}\,(\dot{\epsilon}_e Q/TR)^r \qquad (18)$$

and for n (T, $\dot{\epsilon}$) an, as yet, unknown function. However, another—perhaps larger—difficulty arises due to the fact that the volume (depth of the surface layer) contributing to W_c is not known, nor is the contribution of each volume element to W_c identical, Fig. 5. Thus

$$W_c = \text{const}\,(\sigma_{ts}\, n) \text{ or } W_c \approx \sigma_{ts} n \qquad (19)$$

appears to be at present the best relationship for W_c that can be suggested. In addition to the above-mentioned difficulties, it is r alized that a more specific relation would have to contain geometry and size effects. The size effect is partially due to the fact that more elastic energy (driving force of the crack) can be stored in a larger piece of material. Irwin and Kies[2] analyzed mathematically the case of a centrally notched plate for which many series of experimental results are available. It can be shown that for a given W_c the strength depends on the dimensions of the plate because the dimensions modify the rate of release of elastic energy dW_e/dx as a function of crack length. Thus the balance point, where $dW_e/dx \geq W_c$, can be calculated as a function of x and the applied tensile stress. The calculated crack length x_c represents the condition when the slowly spreading crack becomes a free running crack.

DISCUSSION

For the case of truly brittle fracture we have found previously that $W_c = \gamma$ where γ is the surface energy of the metal. In any other case we may assume that eq (19) applies. It is seen that the tensile strength as well as the work-hardening coefficient play an equal role in the rate with which energy is dissipated. If a case is assumed in which the crack propagation slows down, the value of W_c changes by virtue of its dependence on strain rate. With a decrease in speed of crack propagation, the plastic penetration has more time available and consequently advances deeper into the material increasing W_c. Eventually, we reach the point C, Fig. 5, in which the plate behaves like a tensile specimen[11] at its instability point; necking proceeds along the two shear fronts eminating from the arrested crack-front, Fig. 1*c*. Necked, or even cracked, forks have been observed often at the end points of an arrested crack.[9] In mild steel one often sees only a dimple and Lueder's bands in the direction of the shear fronts.

After the crack is arrested, no external work is being done by the applied forces (mechanical work = 0). The stored energy is now much smaller than originally or the crack would not have slowed down. It is, however, a mistake to assume that no external work is being done at all; in time and under proper atmospheric conditions stress corrosion may cause the crack to grow. An experiment in a constant load machine with proper attention to end condition (attachments) should give an indication at which rate chemical energy is transformed into mechanical. Irwin and Kies[2] estimate that W_c may become smaller by a factor of 10 under unfavorable conditions such as corrosion and/or fatigue.

Copson,[12] in a comprehensive report, "The Influence of Corrosion on the Cracking of Pressure Vessels," states that there is no way of knowing whether an alloy will resist stress-corrosion cracking in a given environment except by trial and error. Other experimenters have found that, under corrosion conditions and steady stress, the time needed to propagate a crack a given distance is a power function of the stress; i.e., plotting stress versus time on log-log paper should result in a straight line.

CONCLUSIONS

1. Fracture toughness (W_c) is defined as the total energy (surface energy plus energy of plastic deformation) needed to form a pair of new unit surfaces; it also equals the driving force needed to move a unit length of crack-front (in.-lb/in.^{2} or lb/in.). It is an engineering property of the material which is inversely proportional to the susceptibility of the material to crack propagation.

2. Fracture toughness (W_c) is shown to be proportional to the product of tensile strength (σ_{ts}) and work-hardening co-

efficient (n). Since both of these quantities depend on strain rate and temperature, it follows that W_c is also strain rate and temperature dependent.

3. Direct experimental evaluation of W_c has not yet been accomplished. Some of the difficulties are a consequence of the following point.

4. Steady rate crack propagation is possible only if all three of the following conditions are satisfied:

(a) The material is homogeneous with respect to the mechanism by which cracks initiate.

(b) The geometrical configuration of the crack front and its geometrical relation to the structure (i.e., plate) are identical at each successive interval of time.

(c) The strain energy stored in the material in the vicinity of the crack front has identical distribution at successive intervals of time.

5. Steady rate crack propagation has been analyzed mathematically and the shapes of the crack front and chevron markings have been derived.

6. The influence of corrosion on crack propagation is only vaguely understood. No fundamental analysis has come to the writer's attention.

7. The critical stress for a Griffith crack is derived by dimensional analysis.

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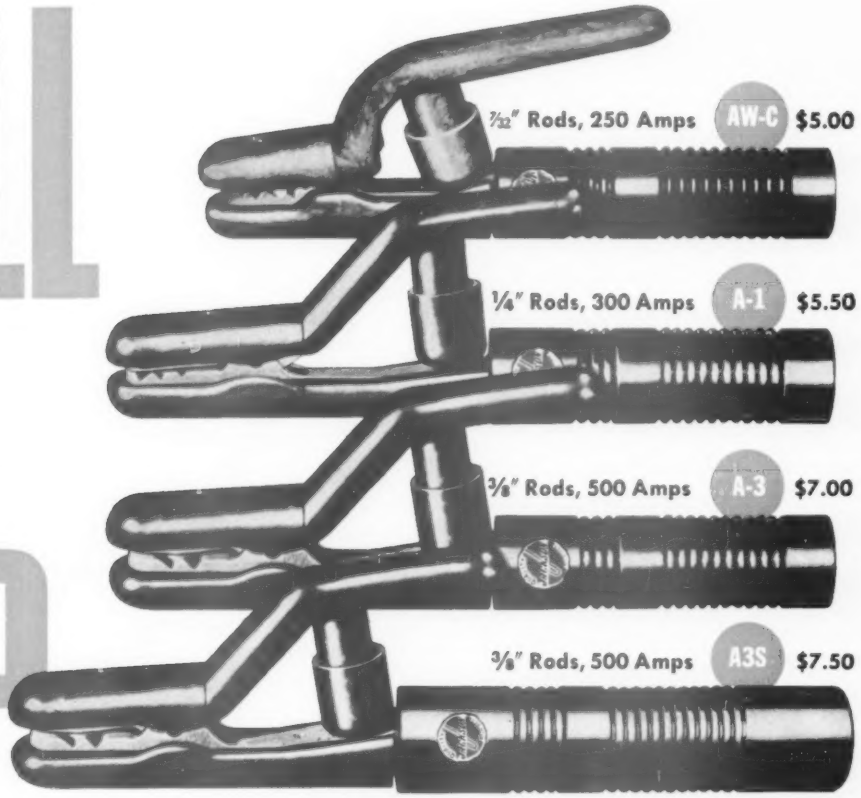

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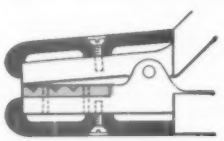

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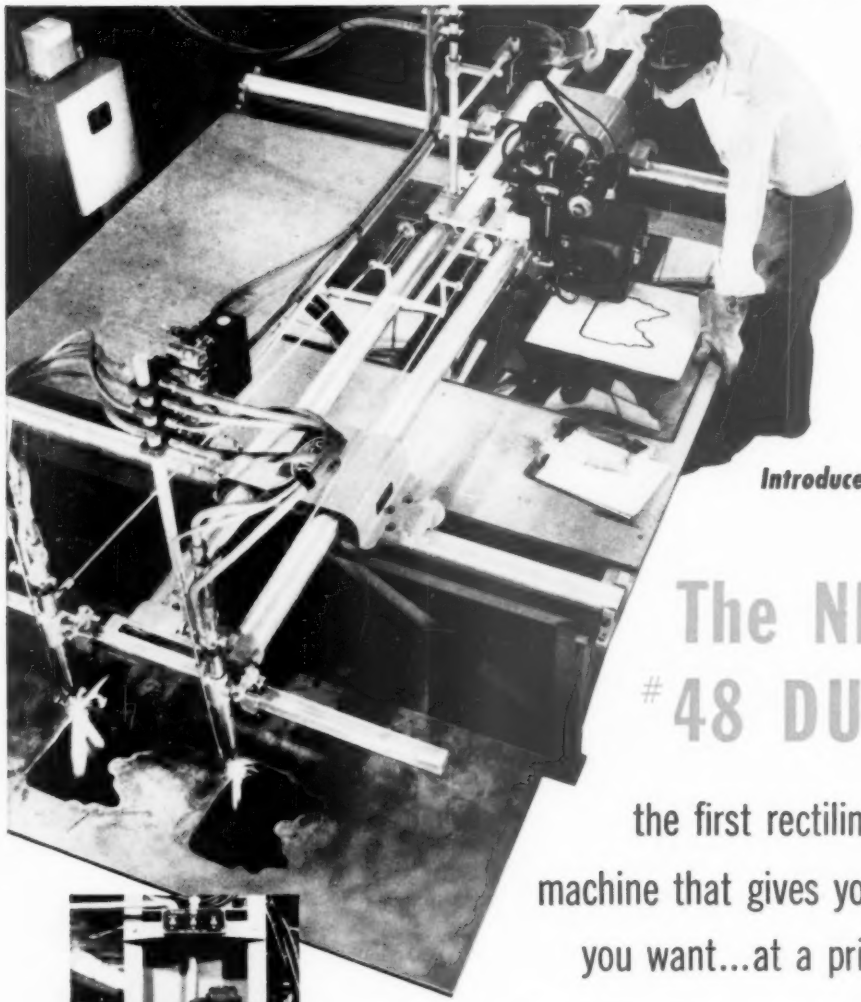

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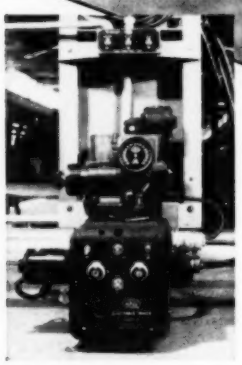

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